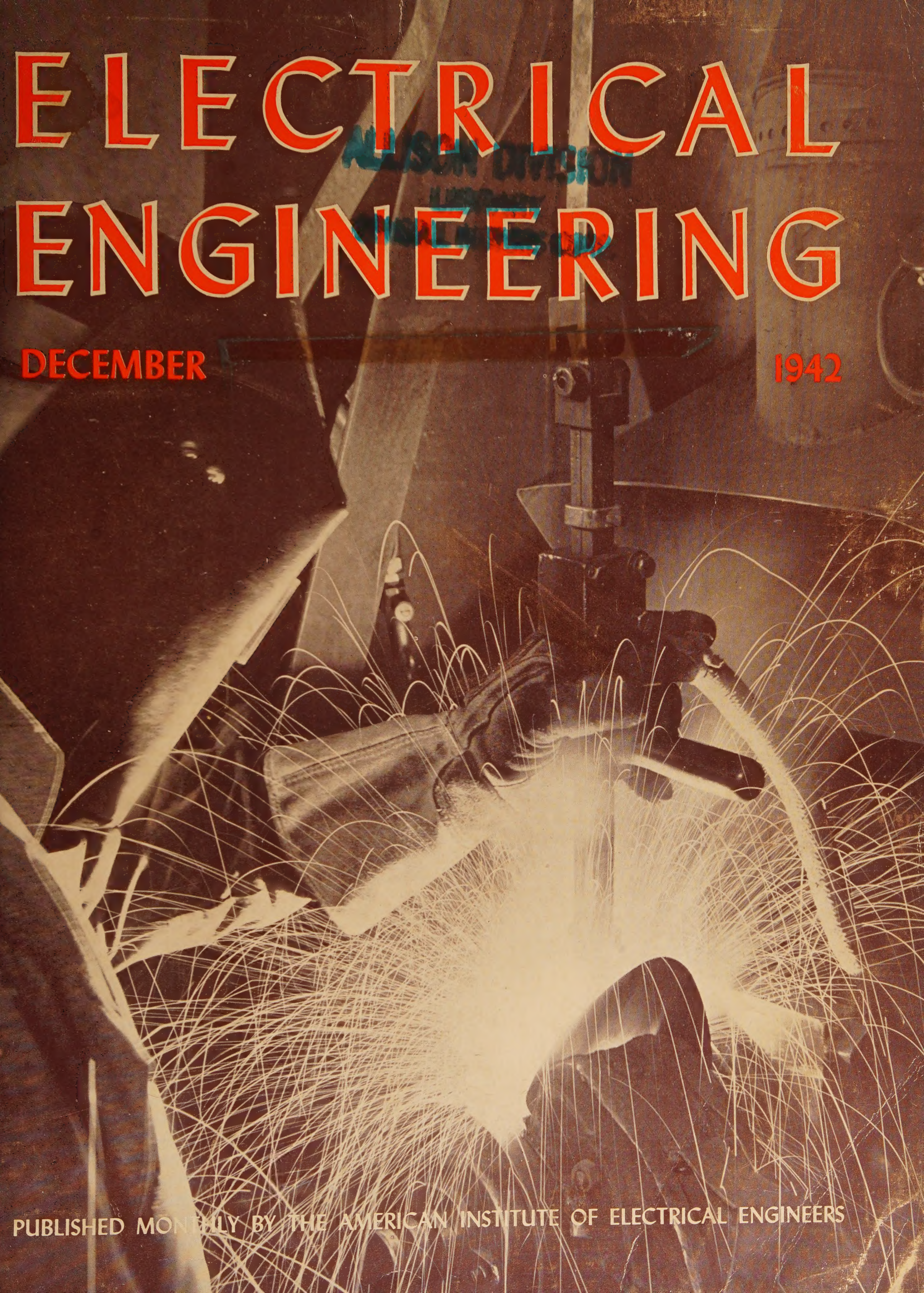
ELECTRICAL ENGINEERING

DECEMBER 1942



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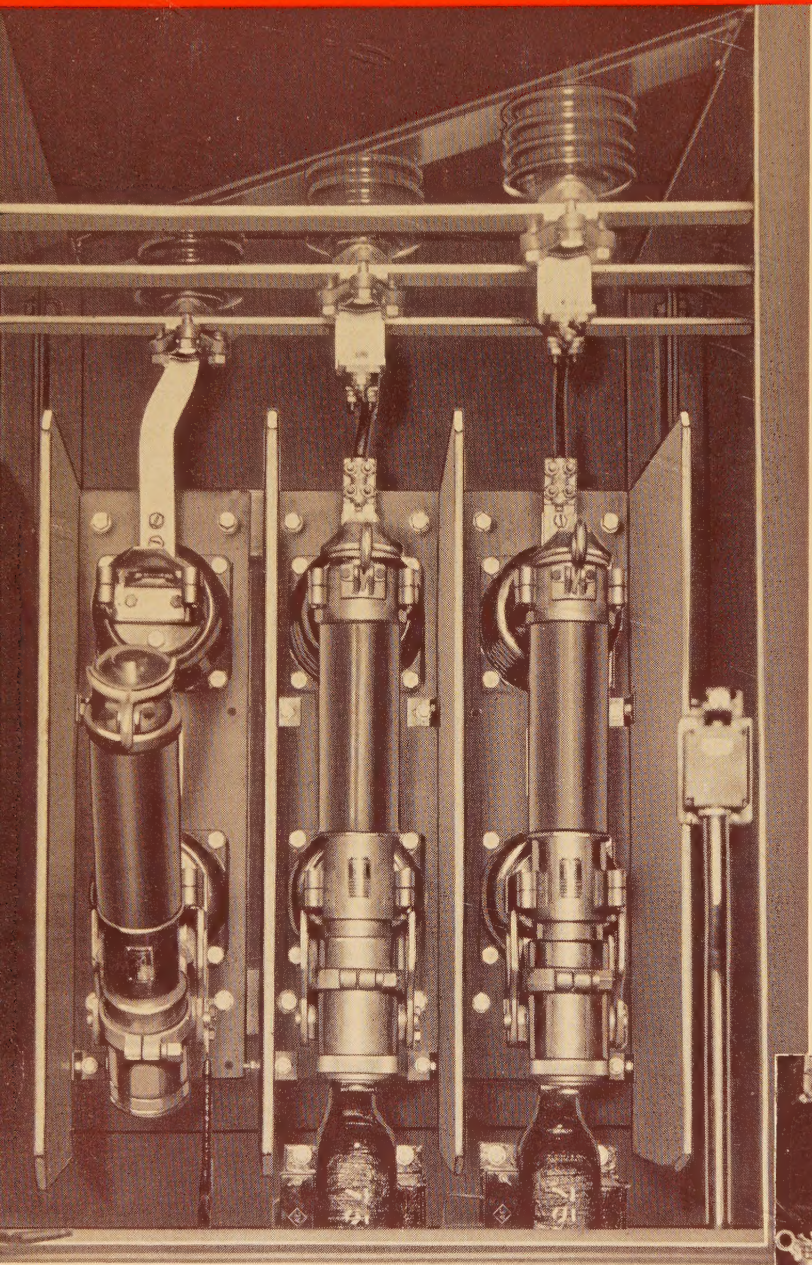

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and	SMP-4	27,500	25,000	20,000	15,000	10,000
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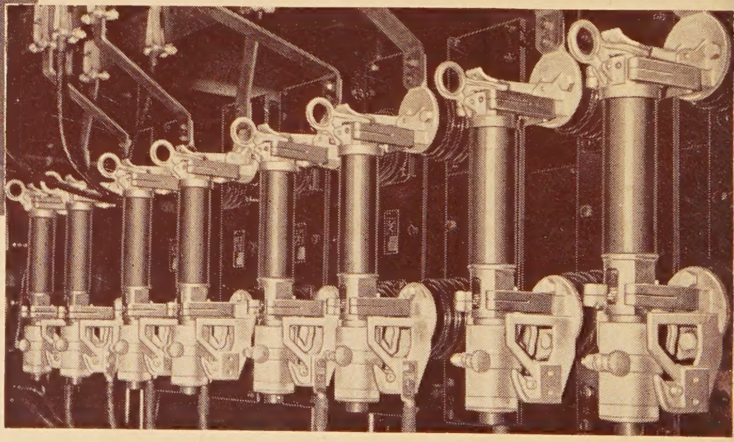

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TYPE "SM"

POWER FUSES

ELECTRICAL ENGINEERING

Registered U. S. Patent Office

DECEMBER

1942

The Cover: Electric welding plays a tremendous part in war production.

Photo courtesy of Westinghouse Electric and Manufacturing Company

TRANSACTIONS SECTION

(Follows EE page 638; a preprint of pages 835–892 of the 1942 volume)

VOLUME 61

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HIGH LIGHTS • •

Air-Blast Breakers. Two papers on air-blast circuit breakers in this issue describe: (1) short-circuit tests on Canadian installations ranging in rating from 4 to 230 kv (*Transactions pages 859–63*); principles of design and operation of high-voltage air-blast circuit breakers, and the construction of two air-blast breakers rated, respectively, 150 and 220 kv (*Transactions pages 869–75.*)

A-C Wave Form. With the increasing necessity of wave-form standardization, different standards have been developed in different fields, as in power, communication, and insulation. While standards, once set up, should remain fairly stable, they should also be subject to review and occasional change to keep in step with technological advances (*Transactions pages 864–8*).

Ring-Bus Characteristics. Network-analyzer studies on a transmission system having a three-winding transformer ring bus and on the same system using a standard high-voltage bussing arrangement proved that transient stability limits are higher and fault currents lower for the three-winding transformer ring-bus system (*Transactions pages 848–9*).

A-C Network Analysis. A conventional d-c calculating board may be used to solve complex problems of load distribution and voltage conditions for a-c networks. For short-circuit work this method can be used to advantage where (1) the resistance of the network circuits is appreciable, so that a network setup representing reactance does not give sufficiently accurate results, and (2) a setup representing impedance is inaccurate because of differences in ratio of X to R for the circuits forming the network (*Transactions pages 875–80*).

Single-Pole Service Restorer. A single-pole service-restoring device utilizes an application for the prestoring of operating energy and the restoration of the utilized operating energy after transient fault conditions (*Transactions pages 889–92*).

Lightning Protection. Basic principles of adequate protection for hazardous structures are gained from experience with similar shielding of electric-power systems, says a lightning engineer. Proper location of masts or ground wires, necessary sizes of conductors, necessary clearances between parts of the shielding system and the structure being protected, and ground resistances obtained with different types of grounds are considered (*pages 591–7*).

Power-System Interconnection. Interconnection of three important hydroelectric systems in Canada, made necessary by war needs, involved combining power resources of nearly 4,000,000 horsepower and has increased the combined firm power capacity far beyond the sum of the capacities of the individual systems when they were operated independently (*Transactions pages 841–7*).

120-Kv Oil-Filled Cables. Design, manufacture, and installation of a 120-kv oil-filled cable system recently installed in Canada as a link in a large interconnection scheme are described in a paper in this issue. Functions of the various sections and reasons for choice of method of effecting the interconnection are explained (*Transactions pages 881–9*).

Inverse Functions. Formulas for inverse functions of complex quantities, such as $\sin^{-1}(x \pm iy)$, are of use in calculations for transmission circuits, particularly in connection with communication circuits (*Transactions pages 850–3*).

Transformer Differential Protection. Tolerances in the selection of current transformers and factors affecting the selection of relays for that purpose are considered in a discussion of the protection of interconnected wye and zig-zag transformers and Scott-connected transformers by the usual current-differential method (*Transactions pages 854–8*).

Correction. In the first paragraph of column two of M. Hochstadter's letter to the editor on compression and oil-filled cable versus gas-filled cable (*EE, November '42, p. 586*) the second and third sentences should read: "European engineers are certainly biased in this matter as they believe in the necessity of a lead sheath. In fact they have hitherto abhorred designs . . ."

Energy Flow in Electric Systems. Examples of the commonly used *Vi* energy-flow postulate show it to be not generally valid, but by adding a simple term it can be made equally valid with other valid energy-flow postulates. The application of this corrected energy-flow postulate is demonstrated (*Transactions pages 835–41*).

Hunting. An electronic circuit for determining power-angle oscillations in synchronous machines during periods of hunting is described in the AIEE national Branch prize paper for the academic year ending June 30, 1941. The method is said to have certain advantages over previous methods (*pages 603–06*).

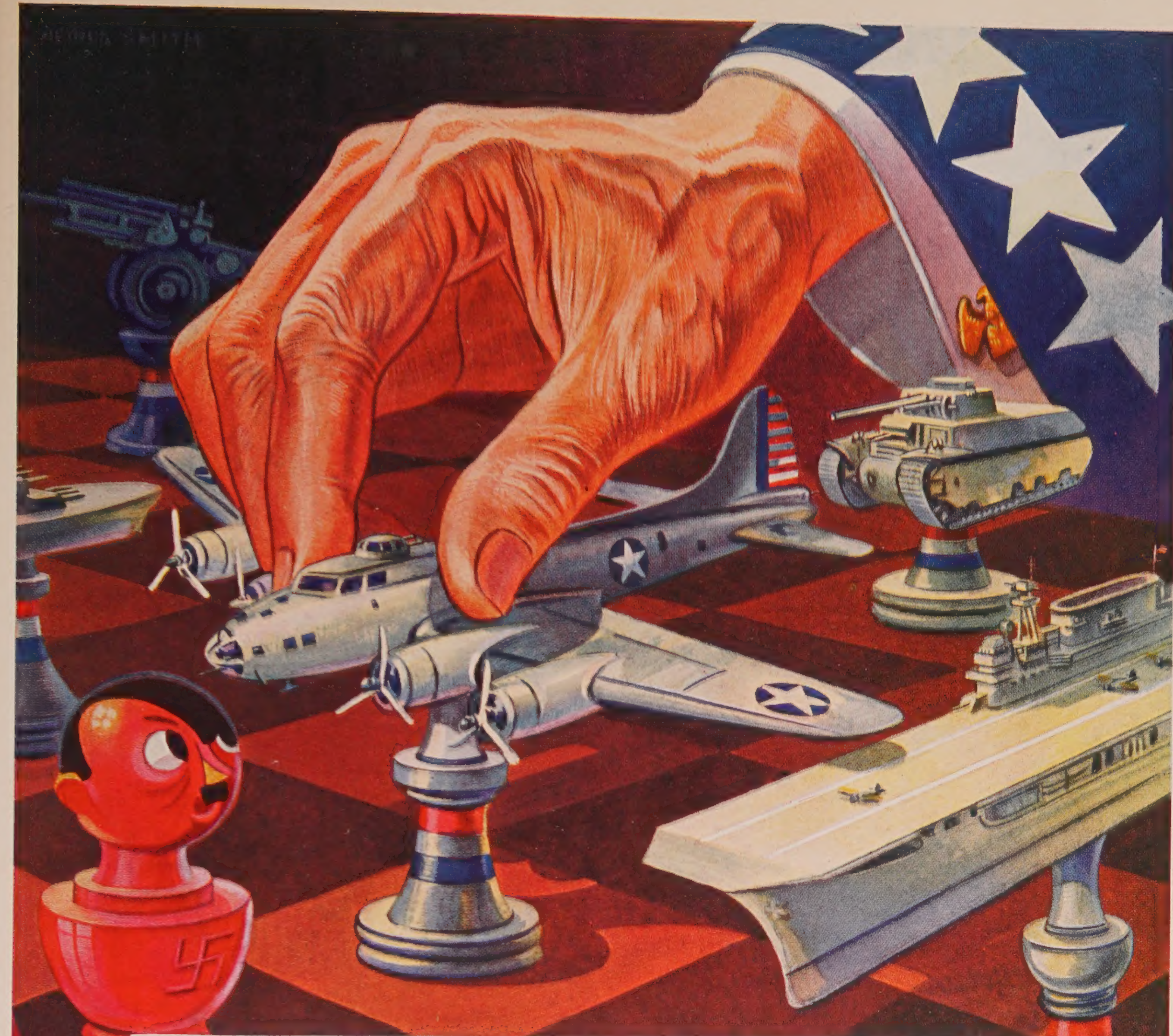
Developments in Switchgear. The history of switchgear apparatus in Europe and America and the present trends in development of switchgear on both continents have been analyzed in the light of current needs and facilities. The requirements of each of three major classes of switchgear are considered individually (*pages 609–13*).

Wave Guides. Methods for determining the shape and size of wave guides for ultra-high-frequency transmission and the mode of transmission to be used are discussed in an article in this issue. Rectangular and cylindrical guides as well as transverse-electric and transverse-magnetic modes of transmission are described (*pages 598–603*).

Mica. Domestic resources of mica, at present an urgently needed raw material, are being developed. In an article in this issue the chief mica sources are pointed out and the production and classification of the various types of mica are discussed (*pages 607–09*).

"I Am an American." An AIEE member who is a naturalized American citizen of Armenian background tells what being an American means to him (*pages 614–15*).

Coming Soon. Among the special articles and technical papers now in preparation for early publication in *Electrical Engineering* are: an article on inventions and the engineer by W. E. Crawford (F '36); a description of the improvement of the lighting in the auditorium of the Engineering Societies Building by S. W. Bruun (A '40); a discussion of power facilities and problems in South America by R. P. Crippen; an outline of power-line transposition practices by F. Von Voigtlander (M '38); an analysis of a practical equivalent circuit for a five-winding transformer by L. C. Aicher, Jr. (A '37); a report on the correlation of overvoltages with system-grounding impedance by an AIEE group; a study of hunting and self-excitation of a 300-mile transmission line by R. B. Bodine (A '37), C. Concordia (M '37), and Gabriel Kron (A '30); a discussion of equivalent circuits for oscillating systems by Gabriel Kron (A '30); a discussion of a polyphase adjustable-speed motor and its control circuit by A. G. Conrad (M '40), S. T. Smith (Enrolled Student), and P. F. Ordung (Enrolled Student); an analysis of rating methods for intermittent loads by R. E. Hellmund (F '13, deceased); a consideration of the dielectric-recovery characteristics of large air gaps by G. D. McCann (A '38) and J. J. Clark (A '40); a study of representative circuits with and without series-capacitor compensation by H. A. Peterson (M '41) and T. W. Schroder (A '37); a description of an instrument for the determination of contact making and breaking time by Walther Richter (F '42) and William H. Elliot (A '40), and presentation of a method of calculating performance on tapped-winding capacitor motors by P. H. Trickey (M '36).



The Second Vital Move

on the chessboard of global war is beginning—*the offensive!* But this could not have taken place until the first move—the production of the implements of war—was well begun. Management and labor, working together, are delivering tools of war in ever increasing quantities. » » A flood of planes, ships, tanks and other mobile units is pouring from American factories. Okonite is making many of the insulated wires and cables on which they depend for power, control and communication. Even the dearth of rubber has not stopped us, because our research laboratories years ago learned how to use synthetics and other materials for insulation. » » Without the constant faith in our country's future that spurred us and hundreds of other American Industries to maintain these research laboratories—our armed forces might still be waiting for the necessary tools of Victory.

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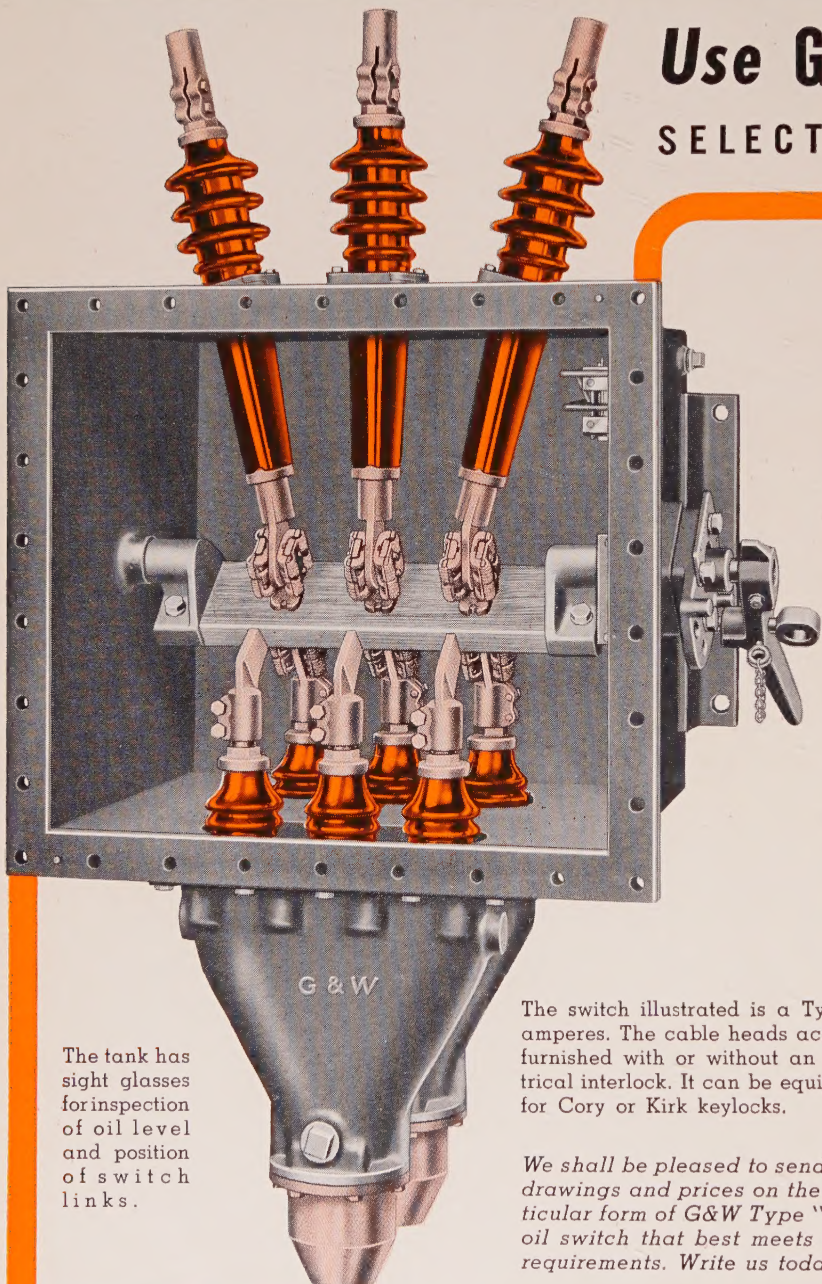

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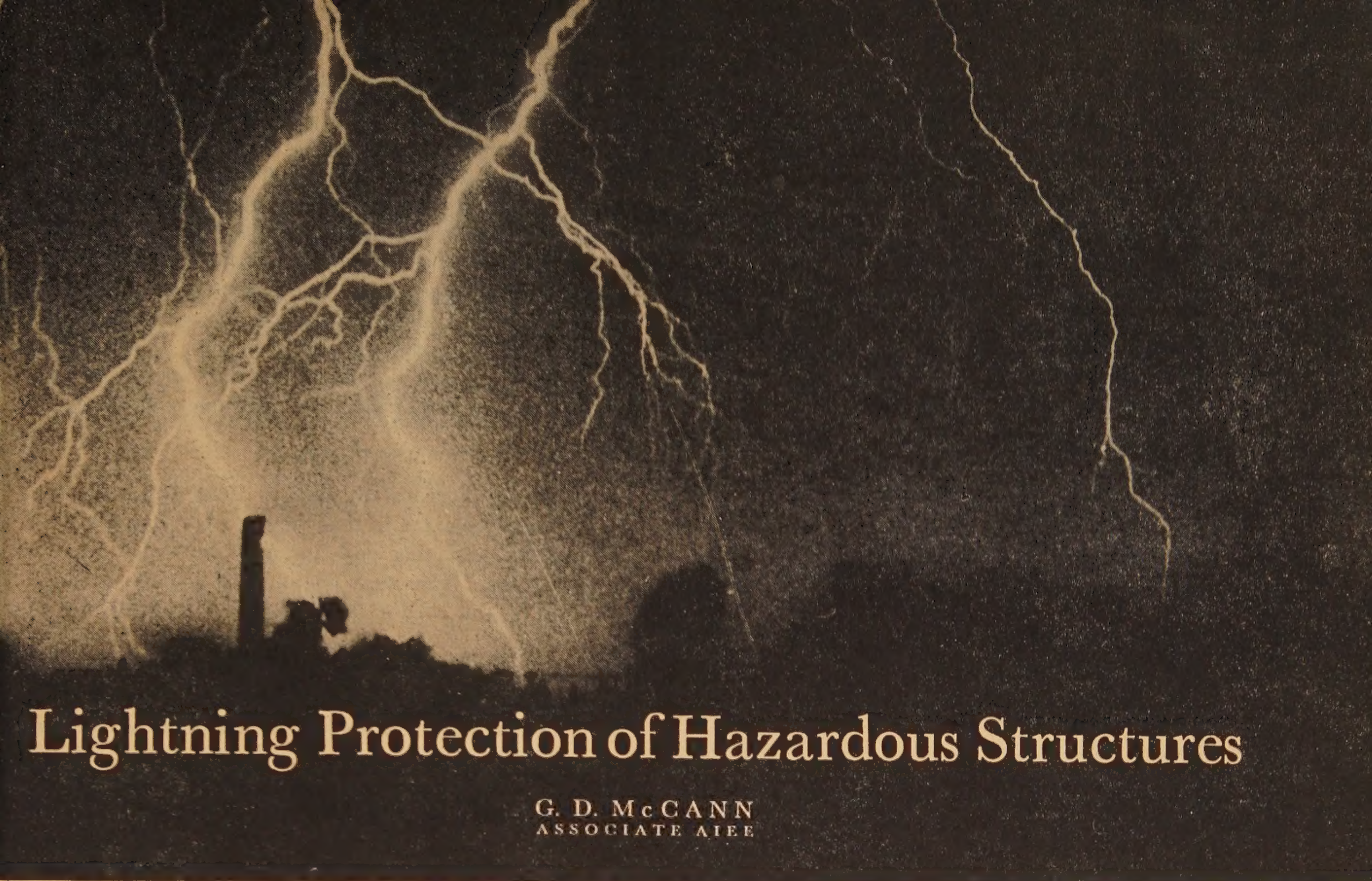


Lightning Protection of Hazardous Structures

G. D. McCANN
ASSOCIATE AIEE

Separately mounted shielding systems, such as vertical diverter masts or ground wires, are best for most hazardous buildings, because they produce adequate shielding and sufficient clearances to prevent sparking. Cost of materials and installation are less than with the use of lightning rods, since conductors for the masts and ground wires are smaller, and corrosion-resisting steel wire can be used instead of copper. Location of masts or ground wires, sizes of conductors, necessary clearances, and ground resistances are considered in this article.

OUR present knowledge of the characteristics of natural lightning and the protection of electric-power systems, gained from operating experience and the extensive investigations conducted during the past 25 years, has provided considerable information which can be applied to the protection of structures other than transmission lines and substations. Lightning rods have been used for many years for a wide variety of buildings and other structures and have considerably reduced lightning damage. As the realization has been reached that their true function is to protect against direct strokes and not to prevent them, and as the importance of obtaining good grounds has become better appreciated, the efficiency of this type of protection has been increased. Experience indicates that most of the common types of structures, such as rural farm buildings and the like, can be protected to a sufficient degree with the conventional application of lightning rods mounted directly on the structure, as long as they are correctly installed in accordance with the "Code for Protection Against Lightning."[1] The degree of protection necessary depends upon property value, vulnerability or hazard, and probability of direct strokes. The best possible protection is of considerable importance for structures in which petroleum products, inflammable gases, and explosives are being handled or stored. A very large number of these are being constructed at the present time throughout the country. Protection methods which have been found sufficient for ordinary buildings are not necessarily adequate for these. It is felt that more general application of methods applied to power systems will result not only in better protection, especially for these hazardous structures, but also a saving in critical materials and labor as compared to present methods of protecting such structures.

G. D. McCann is central-station engineer with Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

PROBABILITY OF DIRECT STROKES

The variation of lightning conditions throughout the United States is best shown by the isoceraunic map of

Figure 1, which gives the average over a 30-year period of the number of thunderstorm days each year occurring in different parts of the country. For the eastern part the average is about 30 storm days per year, a value that is usually taken as a basis for comparison. The frequency and severity of storms varies considerably from year to

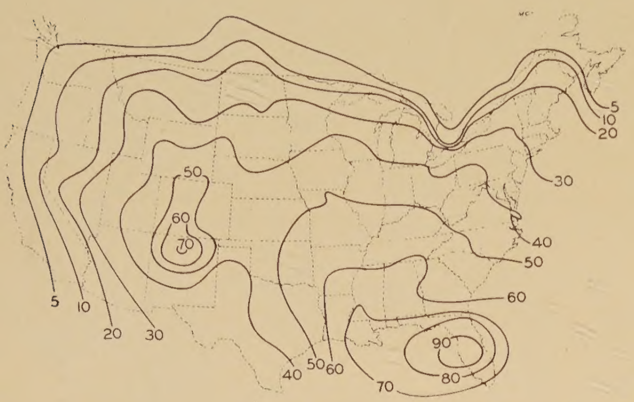


Figure 1. Annual isoceraunic map showing the number of thunderstorm days per year (Alexander[2])

year and in any region with local conditions of terrain and storm paths. However, it is possible to determine the order of magnitude of the frequency with which objects are likely to be struck and the factors which influence this. Through lightning field investigations[3] such records have been obtained for structures in regions of isoceraunic storm levels lying between 25 and 40. These data are given in curve *A* of Figure 2 and show that below about 500 feet the probability of strokes is about proportional to height. Supplementary data[3] obtained from laboratory model studies have determined the effect of the horizontal dimensions of the structure. The area it covers is the principle factor as shown by curve *B* of Figure 2. For buildings whose horizontal dimensions are of the same order of magnitude as their height, area has little effect, and curve *A* can be used directly. Objects very low in relation to the area which they cover, such as oil reservoirs, are not likely to attract strokes from a sky area larger than their own area. The probability of direct strokes is directly proportional to the area of the structure and the number of strokes which emanate per unit area of sky. Data obtained on strokes to transmission lines together with laboratory studies[3] indicate that an average figure for the number of strokes emanating each year from a square mile of sky area in regions of isoceraunic storm level of 25 to 40 is about 9.5. For estimating purposes the expectancy of strokes in regions varying considerably in the number of storms than that to which the curves of Figure 2 apply can be obtained by using the direct ratio between the respective isoceraunic levels.

The data of Figure 2 apply to structures situated on essentially level terrain and sufficiently far from other objects so that they will not shield it. The shielding provided by such adjacent objects is treated in a later section. Curve 2*b* shows that an average sized structure 25 feet in height may be struck as often as once every 30 or 40 years, while one of large extent having an area of, say, 40,000 square feet, about once every ten years. Some of the modern ordnance plants may have several hundred buildings so that there is a good likelihood of several in each plant being struck each year. The oil reservoirs of tank farms frequently cover as much as one tenth to one quarter of a square mile. Thus, there is a probability that one to three direct strokes would occur each year if they were not adequately shielded.

SHIELDING

Some types of structures, such as metal tanks, are essentially self-protected in that they provide an adequate continuous conducting path for a stroke which would contact them. The most complete protection of any structure or any portion of a structure for which this is not the case would be obtained by providing a complete metallic shell around it which would be struck instead. This is seldom practical, however, and recourse is usually taken to placing a few well-grounded objects, such as vertical conducting masts or horizontal ground wires, either above or around it, to provide a sufficient degree of protection. Their proper location in order to provide such adequate shielding has been a subject of considerable study in connection with the protection of transmission lines and substations. Standard types of lightning rods as commonly installed on the ridges and elevated points[1] usually provide sufficient shielding for most structures. However, this procedure does not always provide the degree of shielding necessary for hazardous structures. As a result of laboratory studies with models and operating experience,[3,4] the necessary locations of masts and ground wires relative to exposed points have been determined as a function of their efficiency of shielding. In Figures 3 and 4 are given data for the determination of the configuration to provide what is considered to be the best practical shielding. Figure 3*a* applies to the use of a single vertical mast. The necessary height (y) in feet above any point of a structure to be shielded is given as a function of the height (d) of that point above ground and its horizontal distance (x) from the shielding mast. In

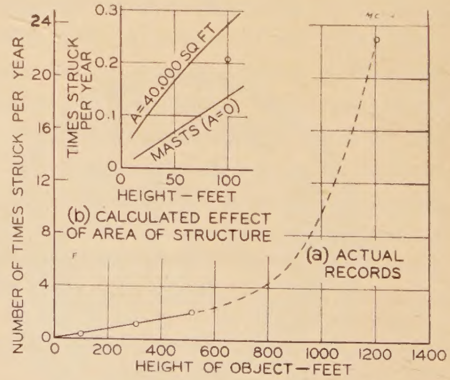


Figure 2. Number of times per year objects of various heights are struck by lightning[3]

Figure 3*b* are similar curves for a single horizontal ground wire. When more than one mast or ground wire are used close enough together that the shielding areas overlap, the curves of Figures 3b and c apply. The region in which various combinations of masts or ground wires will protect objects of a given height is shown in Figure 4.

In the use of these data, configurations of masts or wires can first be chosen so that the higher and apparently more exposed points of the structure lie within the area of shielding as determined from Figure 4. These points will generally be close enough to the same height that one value can be used for *d*. The necessary elevation *y* of the shielding system can be determined from the curves of Figure 3 so that the dimensions *x* and *S* provide an area sufficient to protect all of these points. The adequate protection of lower points that may lie outside of the area so determined can then be checked by determining the larger area of protection for the new height (*d*) and correspondingly greater elevation (*y*) of the shielding system above these points. This will be illustrated in examples given later.

NECESSARY SIZE OF GROUND LEADS

All of the shielding circuit which conducts the lightning current to ground, including the aerial ground wires or the terminating points of vertical masts, the ground leads connecting them to ground, and the driven rods or buried conductor constituting the grounding, need to be of sufficient current-carrying capacity for lightning-stroke currents. In the past[1] it has been customary to specify that, if the ground leads are copper, they should have an equivalent-current carrying capacity of about number 2 American wire gauge conductor which is approximately one-quarter inch in diameter. If galvanized steel is used the equivalent of a three-eighths inch diameter conductor is specified. It is now realized,

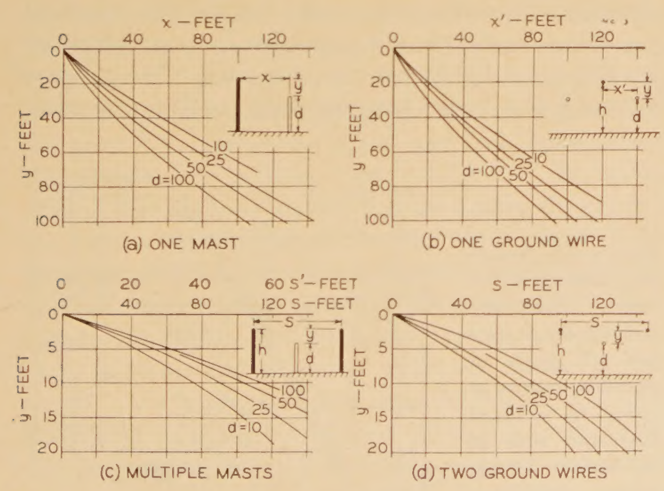


Figure 3. Curves for determining the location of masts and overhead ground wires to shield a structure adequately

Necessary height (y) of shielding objects given as function of height (d) of protected structure and the horizontal distances x and S which determine regions protected

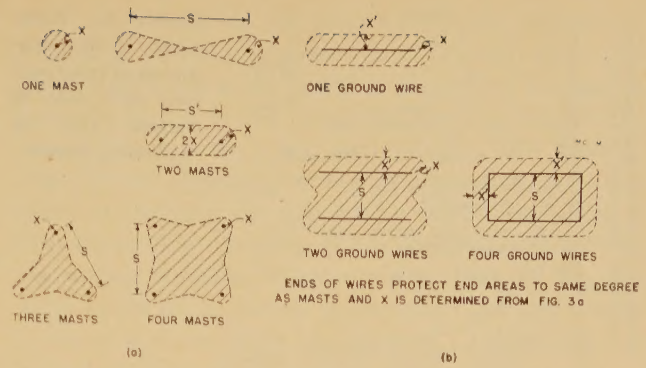


Figure 4. Regions in which points at a given height (*d*) are protected by various combinations of shielding masts and overhead ground wires

however, that a number 6 American wire gauge conductor in either copper or steel has more than the necessary current-carrying capacity and sufficient mechanic strength for most applications. Mechanical strength and corrosion are the primary limitations in size. Where overhead wires are used for shielding, however, greater fusion due to arc contact occurs. It is thought that the minimum conductor size which is safe to use for overhead wires is number 2 in either copper or galvanized steel. Stranded conductors are usually best. The terminal points of masts do not need to be so large, since the fusion upon stroke contact would only fuse at the most, a few tenths of an inch of the tip. A number 6 wire, if properly supported, is adequate, and special high-conductivity tips are not required. About three-eighths-inch rods are the smallest that can be driven, and any standard ground rod has adequate current-carrying capacity. Data[5,6] are now available on the necessary thickness of sheet metal in order that holes or appreciable pockets will not be fused by stroke contact. Sheets greater than about three-eighths inch thick cannot be appreciably damaged by the most severe stroke. Thus, metal tanks and the like, which are self-protecting in every other respect, do not require shielding if constructed from metal of at least this thickness.

The use of the smaller conductor sizes recommended here results in a considerable savings in materials which are critical at this time. Galvanized steel used instead of copper can represent a considerable saving in the more vital metal.

NECESSARY CLEARANCES

Any region completely enclosed by metal surfaces is shielded not only from contact by direct strokes but also from the fields of any electrical disturbance emanating from without. Potential differences for producing sparking cannot be developed. Metal storage tanks which are completely sealed off would require no form of protection for their contents. Grounding of the tank is only necessary to prevent possible injury or damage on the outside. Sufficient bonding of all surfaces which are not continu-

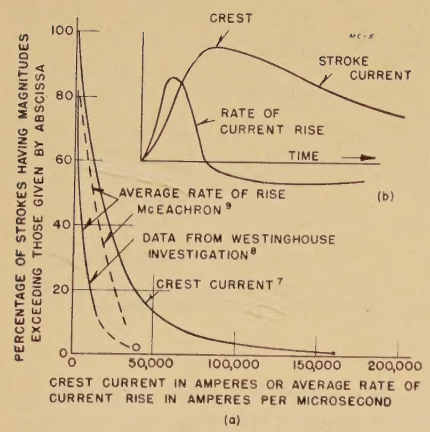


Figure 5. Probability of the magnitude of crest current and rate of current rise in lightning strokes

ous and the proper application of flame arresters at all vents of tanks containing inflammable material usually constitutes sufficient protection.

Little attention need also be given to the contents of buildings whose roofing and siding surfaces are metallic and adequately bonded. However, many cases of hazardous buildings have been encountered where bonding was not provided. In some cases the metal sheeting used was coated with an insulating material so that bonding was very difficult. Unbonded buildings of this type in most cases provide sufficient shielding from electric fields and sparking in their interior, but hazardous sparking is liable to occur between sheets of the material. This is most likely to occur when the shielding system is mounted directly on the structure. Then arcs of high current from the stroke with sufficient energy to constitute a fire hazard can be produced. This hazard is greatly reduced when the shielding system is moved away by separate mounting, as will be discussed later. However, in some cases, low energy sparks can be produced even by indirect strokes occurring in the vicinity of the structure.[7] For any building in which this would constitute an explosion hazard, such construction should not be used unless adequate bonding is provided.

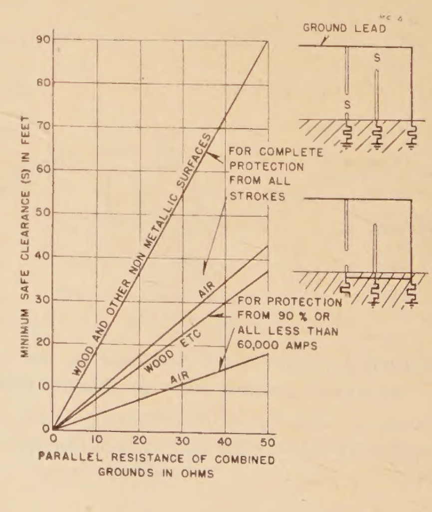
The effectiveness of such interior shielding from electric fields decreases as the amount of metallic surface is decreased. However, practically all buildings constructed of reinforced concrete or structural steel for the roof, floor, and all sides, all bonded together, provide adequate interior shielding. The only necessary requirement is that a continuous metallic path be provided from the stroke terminal point to this metallic grid and then to ground, so that no sparking will be produced that might crack intervening concrete or otherwise cause a sparking hazard.

When only a relatively few conducting paths are provided to ground, it is necessary that their inductive impedance and the grounding resistance be low enough, relative to the clearances from all metallic objects which might provide another stroke path, that sparkover cannot be developed. Such sparking potentials are determined by the nature of the stroke current and the impedance offered to it by the grounding system. The character of the wave shape of lightning-stroke currents is shown in Figure 5a; the current usually rises in a period from one to ten microseconds to its crest magnitude and then decays to essentially zero value in a period of from 20 to 100 microseconds. The crest magnitude of the voltage produced across a ground can be considered as being equal to the product of its measured resistance and the crest magnitude of the portion of the stroke current which passes through it, or it can be considered in most cases as being equal to the product of the total stroke current and the parallel resistance of all the grounds connected to the shielding system. The magnitude of the inductive voltage across a conductor connecting a ground can be considered as equal to the product of the inductance of the lead and the average rate of rise of the current to its crest. The rate of current rise reaches its maximum and decays to zero by the time the current is maximum, and for all practical purposes the two voltages can be considered as occurring at separate times. The curves of Figure 5b give what is considered to be the best available data on the probability of the crest magnitude and the average rate of current rise in a stroke. The maximum crest is 160,000 to 200,000 amperes, and the maximum average rate of current rise that has been recorded[8] about 40,000 amperes per microsecond. In designing for the best possible protection these figures should be used.

The self-inductance of a ground lead can, to good accuracy, be considered as about 0.4 microhenry per foot of length. The nonconducting paths over which sparking may occur can usually be in two classifications. The first of these is air which has a minimum flashover voltage of about 180 kv per foot of spacing for voltage surges of the wave shape produced by the potential across the grounds and about 370 kv per foot for those corresponding to the inductive drop. For practically all other surfaces, such as wood, stone, brick, and the like, conservative values for the two corresponding flashover potentials are 80 kv and 150 kv per foot of surface. The curves of Figures 6 and 7 have been developed to show the neces-

Figure 6. Necessary clearances between separately grounded objects and all parts of shielding system in order to prevent flashover due to resistance drop across shielding-circuit grounds

Upper set of curves recommended for all hazardous structures



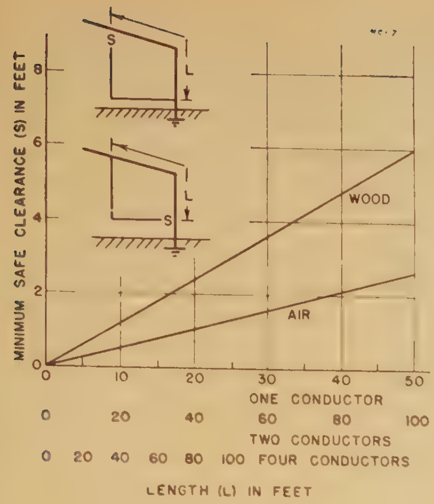


Figure 7. Necessary clearances to prevent flashover due to inductive drop in ground leads

Co-ordinates for abscissa apply to number of ground leads carrying total stroke current to ground

sary clearances which must be maintained to prevent flashover. When the possible flashover gap consists of several in series, the effective length can be considered as the sum of the separate spacings. In Figure 6 is shown the necessary clearances which must be maintained from the shielding system and any objects separately grounded, or the effective clearance necessary to true ground, as a function of the effective combined resistance of the shielding system's grounds.

These necessary spacings are quite large, even for comparatively low ground resistances, such as ten ohms. This illustrates the importance of obtaining low resistance and interconnecting to all grounded objects such as grounded pipes, telephone grounds, and electric power grounds. For cases where metallic objects are not connected to a solid ground but are resting on the ground plane or floor of the building and probably have a high resistance to ground, it is possible in some cases that the potential along the floor of the building will rise appreciably, because of the drop across the lightning ground, so that this full potential difference will not appear between the lightning grounding system and the metallic objects. However, it is not safe to try to take advantage of this, and the minimum safe clearances between a ground or any part of its connecting circuit and such metal objects should be taken as those given in Figure 6. For determining clearances to prevent flashover along the ground, the same curve applying to wood should be used. If the floor is a perfect conducting plane tied into the lightning grounds, the complete potential difference will be eliminated. For cases where lightning rods are used directly on a building and adequate clearance cannot be obtained, such a ground plane may be necessary. This may consist of a bonded reinforced concrete floor. From the standpoint of the potential drop across the grounding system, metal objects in the building do not then have to be grounded. However, the elimination of purely static sparks may require their bonding.

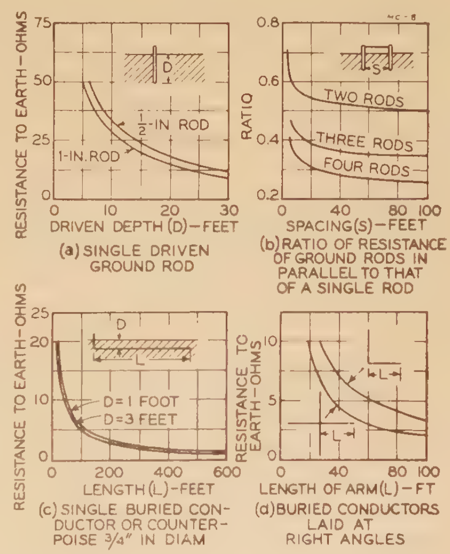
Even after interconnection of grounds or the formation of a good ground plane at the base of the building has been provided, there still exists a possibility of flashover because of inductive drop between grounded objects and the ground leads as illustrated in Figure 7. The rise in potential caused by the inductive drop in a lead can be considerable for the more severe strokes, and sparking can only be eliminated either by increasing the clearances or bridging the gap with a conductor. It is of importance that reduction in this potential difference by the mutual coupling which may exist between the lead carrying the stroke current and a metal object paralleling it can seldom be taken advantage of. Unless they are extremely close, this factor is insignificant.

GROUNDING

It is difficult to specify a general value for the ground resistance which should be provided. In all cases, as shown by Figure 6, the lower the total ground resistance, the better, and all grounds should be made as nearly equal as possible. It is most important to obtain low resistance when interconnection between other grounds and large metallic objects is not provided. Then in practically all cases it should be below about five ohms when the shielding system is mounted on the structure to be protected. When adequate interconnection is provided, the safe upper limit is probably around 20 ohms for the combined parallel ground resistance. When separately mounted shielding systems, such as

Figure 8. Resistance to ground provided by driven ground rods and buried conductors

Calculated from formulas of H. B. Dwight.[10] *Curves apply directly to an average soil resistivity of 100 meter-ohms. For other resistivities multiply resistance by ratio of actual soil resistivity to 100*



separate diverter masts or ground wires, are used, the clearances can be made as large as necessary, and a greater flexibility in ground resistance is permissible.

In Figure 8 are given curves which show the ground resistances that can be obtained with various combinations of driven ground rods and buried conductors or counterpoise. These curves are based upon an earth resistivity of 100 ohms per cubic meter or 100 meter-ohms. As shown by Table I, this is a general average soil condition. The resistance of a given ground is

Table I. Earth Resistivity for Various Types of Grounding Conditions[11]

	Resistance in Ohms Per Cubic Meter
General average	100
Sea water	0.01–1.0
Swampy ground	10–100
Dry earth	1,000
Pure slate	10^7
Sandstone	10^8

directly proportional to earth resistivity, and the curves can be used for any other resistivity simply by multiplying all resistance values by the ratio of the actual resistivity to that of 100. If the data of Table I are not sufficient to form an idea of the earth resistivity in a given region, and no grounds whose configuration is known are available for measurement, a rod can be driven a given depth and measured. Earth resistivity, of course, varies with moisture content and is lowest when this is highest. However, either a driven rod or a buried conductor has its highest resistance just after it is installed, because its surface contact with the earth is poor until packed down by successive rains. Thus, if the resistance is low enough just after installation, it should be satisfactory at all times.

As shown by Figures 8a and c there is a limit to the length of either a driven rod or buried wire beyond which little is gained in decreased resistance. The use of multiple rods driven in parallel does not lower the resistance in direct ratio to the number of rods, unless their distances apart are very great. The effectiveness of parallel rods is shown in Figure 8b. The diameter of a rod or buried wire does not greatly affect the resistance in the practical range of sizes, nor is the variation very great in the resistance of a given counterpoise for the range of depths which is economically practical.

SEPARATELY MOUNTED SHIELDING SYSTEMS

We have seen that where limited clearances are available between the shielding system and the structure being protected, considerable care must be taken to eliminate chances of sparkover, either by proper arrangement of metal objects in the structure and the members of the shielding system, or by their interconnection. Such interconnection has a disadvantage that stroke current paths are produced which are not specifically designed for this purpose and must be carefully inspected to insure that all portions of them are well bonded and of sufficient current-carrying capacity. The closer the stroke current is to all parts of the structure, the greater the fields which it produces, and the greater the care that must be exercised in the prevention of electrostatic sparking from even small insulated metallic objects. Further, it is frequently difficult to find suitable locations on the structure for mounting the lightning rods in order that adequate shielding be obtained.

These problems can be eliminated by mounting the shielding system entirely separate from the structure to be protected. Such a shielding system can consist of vertical diverter masts placed around the structure or overhead wires stretched over the structure and mounted on masts. Illustrations of this are shown in Figure 9. Such a system of protection has the only possible disadvantage of appearance. It is not recommended for less hazardous buildings where this is an important factor. They can usually be sufficiently well protected by conventional lightning rods. For most buildings which are a serious lightning hazard, appearance is not of great importance, certainly not compared to safety. One limitation is thought by some to be the increased cost of such a system. Actually, however, a saving in materials and labor cost can, in most cases, be obtained. The grounding system does not need to be so elaborate, less conductors to ground are needed, and less attention need be paid to the interconnecting and bonding inside the structure itself. Design becomes basically simpler and requires less detailed attention to assure adequate protection. It has been found that in many cases of ordnance-plant buildings, adequate protection cannot be obtained in any other way without a complete redesign of the buildings. This is particularly true of those mentioned previously, whose roofs and sides are covered with insulated or inadequately bonded sheet metal.

EXAMPLES OF THE PROTECTION OF HAZARDOUS BUILDINGS

The simple structure of Figure 9 is sufficiently typical to illustrate the application of the use of separately mounted shielding systems and to compare them with conventional lightning-rod installations. If lightning rods of the normal length of three to four feet were mounted in the usual manner along the ridge of the roof as shown in Figure 9a, adequate shielding would not be obtained. The edge of the roof is at a height (d) 12 feet above ground and a distance (x) of 20 feet from the rods. As shown by Figure 3a, the necessary elevation (y) of the rods must be 16 feet. Thus, they should be 16 plus 12, or 28 feet above ground, or 8 feet in length to protect the edge of the building. If three such eight-foot rods were mounted at 50-foot intervals on the ridge, they would shield the entire building. As shown by Figures 4a and 3c, two masts will shield an area of a width ($2x$) of 40 feet when S' is 50 feet and y is 16 feet.

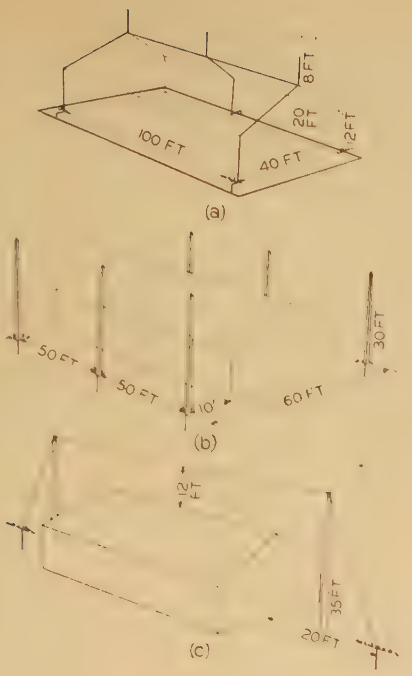
This building can be shielded adequately by the configuration of diverter masts shown in Figure 9b. The necessary distance S between masts on opposite sides of the building is determined by the required clearances from the building. If the floor of the building is reinforced concrete adequately bonded, the mast grounds should be bonded to it, and the mast separation from the building would depend only upon securing clearances for inductive drops in the mast-ground leads. Their closest point on the building should not be over 12 feet from

Figure 9. Examples of lightning protection systems

(*a*). *Lightning rods mounted directly on structure*

(*b*). *Separate diverter masts*

(*c*). *Separate ground wire*



ground, and, as shown in Figure 7, about 0.6 foot is sufficient clearance in air. If the floor is not a good conducting ground plane, the mast ground should be kept a safe distance away as determined by Figure 6. As indicated by this figure, ten feet is sufficient if the resistance for each mast is five ohms. If the resistance is ten ohms, the separation should be 20 feet.

Upon the assumption of ten-foot clearances, the maximum spacing (S) to be used in Figure 3c is 60 feet. For this case the most exposed point is along the ridge at height d of 20 feet. If six masts are used, two essentially rectangular shielded areas are provided as shown by Figure 4a. For S of 60 feet and d of 20 feet, Figure 3c indicates the necessary elevation y of the masts above the ridge of six feet. Thus the mast heights should be 26 feet.

A single overhead ground wire as illustrated in Figure 9c can also be used. Referring to Figure 3b, to shield the edge of the building for which x' is 20 feet and d 12 feet, y should be 20 feet, or the minimum height of the ground wire above ground should be 32 feet. This provides a clearance of 12 feet from the building. If a grounded object extended to the roof, this would still provide adequate clearance to prevent flashover due to inductive drop as shown by Figure 7 for two ground leads of 90-foot length, which is approximately the distance from the center of the ground wire to ground. If the shielding grounds are not interconnected to the building, Figure 6 shows that, for 12 feet clearance in air, the parallel resistance of the two grounds should not exceed 12 ohms, requiring about 25 ohms for each.

The masts for Figures 9b and c can be constructed from standard wood poles. A 35-foot pole would provide a height of 30 feet for the diverter masts, and a 40-foot pole, 35 feet for the ground wire, allowing three feet for sag which is more than ample. Number 6 American wire gauge galvanized steel, extending a few inches above the pole top, can be used as ground leads for the diverter masts of Figure 9b. The horizontal ground wire could be number 2 galvanized steel. The necessary guys at the poles can be used as the conductor to ground. It is only necessary that this guy or the ground itself have the clearances specified by Figure 6. The insulated poles can be as close as is consistent with this. For normal soil conditions, driven ground rods are all that is necessary for the separate shielding systems. A buried counterpoise might, however, be necessary for the rod installation of Figure 9a.

With the exception of the extended rods, the system of Figure 9a is similar to what has in the past been commonly specified for hazardous buildings for which at least three-eighths inch copper conductors have been used. Exclusive of the metal used in the interior of the building for bonding, this would require about 250 pounds of copper. The mast system of Figure 9b would require only about 12 pounds of steel conductor and 40 or 50 pounds of ground rods, together with six 35-foot wood poles which, installed, cost only about $30 each. The ground-wire system of Figure 9c requires about 50 pounds of conductor and guys, 12 pounds of ground rods, and two 40-foot poles whose installed cost is about $35 each. The ground wire would provide the least expensive installation for this particular structure. The use of either diverter masts or ground wires permits considerable fluctuation of configuration in order not to interfere with driveways and the like placed around the structure.

Such systems have been used with good results for substations, oil storage reservoirs, and some ordnance buildings. However, their more general use for ordnance buildings which are now being constructed so extensively, would greatly reduce the hazard of such structures and result in a considerable savings in critical materials.

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Ultrahigh-Frequency Transmission in Wave Guides

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Ultrahigh-frequency, or microwave radiation is being employed more and more in the prosecution of the war, for communication and other specialized purposes. Unlike the power and audio frequencies, transmission in the ultrahigh-frequency region is accomplished best by wave guides, or tubular conductors. In order to make practical use of wave guides, it is necessary to decide upon the shape and size of the guides and upon the mode of transmission to be used. Here transverse-electric and transverse-magnetic modes of transmission in both rectangular and cylindrical wave guides are considered.

GUIDED TRANSMISSION as a term may apply to any system in which electric power is constrained to follow a conductor as in the open-wire, coaxial, or wave-guide line. The name wave guide, however, has come to refer to the system whereby an electromagnetic wave is constrained to move through a tube without a return conductor. In speaking of this method of transmission it is first necessary to review briefly the characteristics of an electromagnetic field in the vicinity of a perfect conductor. In the first place, in such a field the tangential component of the electric field at the surface of the conductor must be zero. Also, in general, the electric field must be perpendicular to the magnetic field at all points.

An approach may be made to the treatment of wave guides by considering an ordinary open-wire line where the transmitted power is subject to changes produced by the line parameters and to loss by radiation. As the frequency is indefinitely increased, radiation and resistance will result in a comparatively high attenuation. The radiation may be reduced by shielding and a coaxial cable results, in which the entire field is constrained to remain within the outer conductor. At very high frequencies the current flows in a very thin layer on the inside of the outer conductor and on the outside of the inner conductor. In the wave guide the field is also entirely contained within the hollow conductor and whatever current exists will flow on the inner surface of the guide. This article treats some practical aspects of the subject but is not concerned with the derivation of the basic equations needed. The derivation of these can be found in many references at present available.[1–6] A knowledge of ordinary line-transmission theory is assumed.

RECTANGULAR GUIDES

The rectangular wave-guide tube has dimensions as shown in Figure 1 where the direction of transmission is to be along the z co-ordinate. The horizontal dimension is taken as b and the vertical as a. The type of transmission to be considered is characterized by the presence of the electromagnetic field components, E_y, H_z, H_x, where E and H are the field intensities in volts per centimeter and amperes per centimeter respectively (Figure 2).

The value of β, the phase constant, for this transmission can be shown to be

$$\beta=\sqrt{(\omega/c)^2-(\pi/b)^2}$$

where b is the width of the tube in centimeters and c is the velocity of light. The lowest frequency which can be transmitted is given by setting $\beta=0$. That is

$$\omega_c/c=\pi/b$$

or

$$f_c=c/2b$$

where f_c is the cutoff frequency. For values of f lower than f_c, β will be imaginary and no transmission can take place. This result leads immediately to the fact that very high frequencies or very large tubes must be used. For instance, if a tube 10 centimeters wide were used, the frequency would have to be at least as high as

$$f_c=3\times10^{10}/20=1.5\times10^9 \text{ cycles per second}$$

Thus wave guides are essentially high-frequency transmission lines and act as high-pass filters.

From the value of the phase constant of the line the following are obtained:

$$v_p=\omega/\beta \qquad \lambda_g=2\pi/\beta$$

where v_p is the phase velocity, and λ_g is the wave length as measured in the guide. The group velocity is given by

$$v_g=d\omega/d\beta=c^2/v_p$$

The above brief outline of the rectangular guide is based upon an exact analysis using the electromagnetic-field equations. In order to see more clearly what the configuration of the field may be within the guide, it is necessary to treat the problem from the electromagnetic standpoint. Let it be assumed that the electric field component is everywhere vertical as mentioned above, that is, in the direction of the y axis. From elementary

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considerations, it is seen that this electric field must be zero at $x=0$ and at $x=b$ on account of the perfectly conducting sides. It is shown theoretically that there is a sinusoidal variation of E from $x=0$ to $x=b$. The fact that the magnetic field must be everywhere perpendicular to the electric field leads to the fact that the magnetic lines must lie in planes parallel to the x–z plane.

It is clear that there may exist both H_x and H_z components, but no H_y component. Since the transmission is to be in the z direction, it is to be expected that H_x and E_y should vary with x in the same manner and have the correct sense to produce propagation in the z direction as given by the evaluation of the Poynting vector. There is no net propagation in the x direction on account of the impenetrability of the walls, thus the field components E_y and H_z must be such that the average value of their product is zero, thus making the x component of the Poynting vector equal to zero. The following set of field components is consistent with the above conditions.

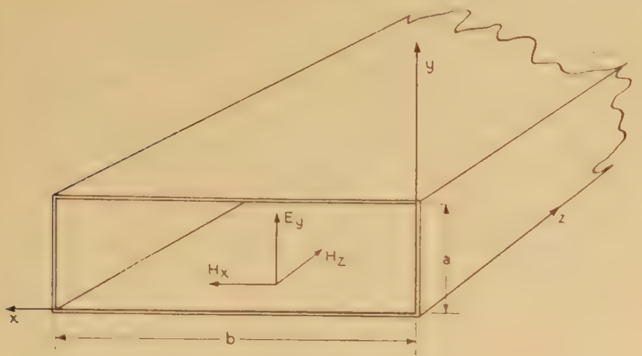

Figure 1. Co-ordinate system for a rectangular guide

$E_y = A \sin \pi x/b$

$H_x = B \sin \pi x/b$

$H_z = C \cos \pi x/b$

In the above expressions the terms involving variations with z and t have been omitted. It is safe to say that z will enter through $\epsilon^{-\gamma z}$ where γ is the propagation constant and t will enter through a sinusoidal term, $\epsilon^{j\omega t}$.

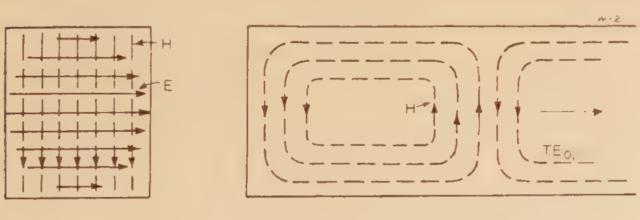

Figure 2. Field configuration for rectangular $TE_{0,1}$ mode

If these exponentials are used, it must be remembered, of course, that the final results must be obtained by taking only the real part.

On the basis of Maxwell's equations γ is calculated to be of the form $0+j\beta$. Thus $\alpha=0$ and β has real values only above a frequency f_c. The results of the analysis are:

$f_c = c/2b$

$\beta = \sqrt{(\omega/c)^2 - (\pi/b)^2}$

$v_p = \dfrac{c}{\sqrt{1-(f_c/f)^2}}$

$v_g = c\sqrt{1-(f_c/f)^2}$

The mode of transmission outlined above is known as the $TE_{0,1}$. If both E_y and E_x components of the electric field are present, other designations are used such as $TE_{1,1}$, and so forth. These terms are defined in a later paragraph.

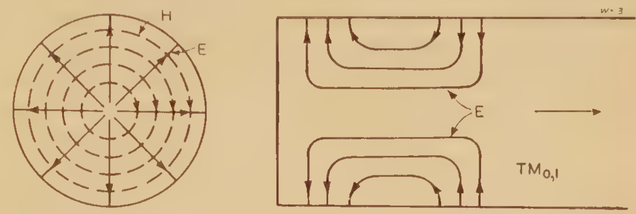

Figure 3. Field configuration for cylindrical $TM_{0,1}$ mode

CYLINDRICAL GUIDES

As for the rectangular guide already considered, the treatment of the cylindrical wave guide is based also on the field equations. The co-ordinate system used is the cylindrical system with r, ϕ, z. The type of transmission known as $TM_{0,1}$ will be briefly outlined below.

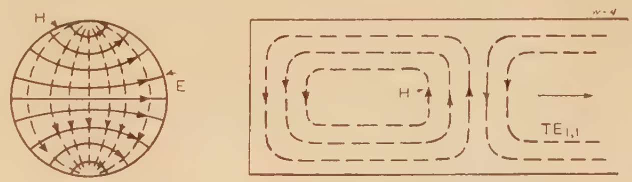

Figure 4. Field configuration for cylindrical $TE_{1,1}$ mode

This mode of transmission is characterized by the existence of only one component of the magnetic field, H_ϕ. This means that the components of the electric field may be E_z and E_r. At the outset the boundary condition demands that $E_z=0$ at the inner surface of the guide. Incidentally, of course, the electric lines E_r must all meet the conducting surface perpendicularly. In a manner similar to that used in the case of the rectangular guide, it can be shown that there must be a certain minimum diameter of tube in order to transmit a given frequency.

This limiting point is given by

$f_c = cp/2\pi b$

where b is the radius of the tube and p is 2.405, the first

root of $J_0(x)$, the Bessel function of the first kind and zero order. The phase constant becomes

$$\beta=\sqrt{(\omega/c)^2-(p/b)^2}$$

The configuration of this field is as shown in Figure 3. Since the $TE_{1,1}$ mode of transmission in cylindrical guides is somewhat similar to the $TE_{0,1}$ mode in rectangular guides, mention of it also is not out of place here. The same basic equations apply to all modes. For instance in this case

$$\beta=\sqrt{(\omega/c)^2-(q/b)^2}$$

where $q=1.84$, the first root of the derivative of $J_1(x)$. Since this is a TE mode, it has no E_z component. However, a certain circular dissymmetry exists as shown in Figure 4.

GENERAL DISCUSSION

The $TE_{0,1}$ mode in rectangular guides has certain advantages which cause it to be especially useful for transmission. In the first place if the tube is made with a dimension b somewhat greater than the critical value and a somewhat less, the tube can transmit this particular mode only and there is no possibility of the wave shifting its polarization through 90 degrees. No other mode of transmission in rectangular guides at the given frequency has as low a value of b at the cutoff frequency. Also this mode has the lowest minimum attenuation of any.

The $TM_{0,1}$ mode has the useful property of being circularly symmetrical and thus it makes no difference if the field rotates as it progresses along the tube.

The mode $TE_{1,1}$ in cylindrical guides has no particular advantage as far as this treatment is concerned. A comparison of the $TE_{1,1}$ wave of Figure 4 with the $TE_{0,1}$ wave in rectangular guides as shown in Figure 2 will bring out their similarity of configuration.

Nomenclature. The method of designating the various modes of transmission which has been introduced is based primarily upon the distinctive elements in the configuration of the field. In any mode there will be at least one component, either electric or magnetic, which is parallel to the direction of transmission. It follows then that either the electric or magnetic field existing within the guide will be entirely transverse. For instance, if there exists a longitudinal component of the magnetic field, the electric field will be a transverse one. The nomenclature is based upon the part of the field which is transverse only. Accordingly, if the electric field is transverse with no longitudinal component, it will be designated as a TE (transverse electric) mode. Similarly a TM (transverse magnetic) mode has only transverse magnetic components while it may, of course, have all electric components. In the case of the rectangular wave guides the subscripts refer to the number of half wave lengths which will fit transversely into the tube at the cutoff frequency. The modes spoken of above as $TE_{0,1}$ and $TE_{0,2}$ thus have no variation in the field along the one dimension here taken as the vertical, but will have a variation in the field of one or two half wave lengths respectively along the horizontal dimension. For cylindrical guides, the discussion must be divided into two parts, one devoted to TE waves and the other to TM waves. The letters TE and TM have the same significance as before. For the TM modes, the first subscript refers to the order of the Bessel function involved, and the second subscript refers to the number of the root. For instance in the mode $TM_{0,1}$ the Bessel function J_0 provides the solution and the root used is the first one, which has a value of 2.405. For the TE modes the first subscript refers again to the order of the Bessel function which appears, but now the second subscript is the number of the root of the derivative of this Bessel function. As an illustration the $TE_{1,1}$ mode involves the derivative of J_1, the Bessel function of the first kind and order unity, designated as J_1', and the first root of J_1' which is 1.84.

PRACTICAL CONSIDERATIONS

In making practical use of wave guides, it is necessary to decide upon three things: (1) shape of guide, (2) size of guide, and (3) mode of transmission to be used. It is well to note again the two primary modes of vibration. The simplest one in rectangular tubes is the $TE_{0,1}$ mode which has a configuration as shown in Figure 2. A useful mode in cylindrical guides is the one designated by $TM_{0,1}$ and shown in Figure 3.

Methods of Excitation. The two modes already mentioned are set up in wave guides in an especially simple manner. Since the rectangular guide has electric lines which are shown perpendicular to the longer dimension, the method shown in Figure 5*a* is immediately suggested.

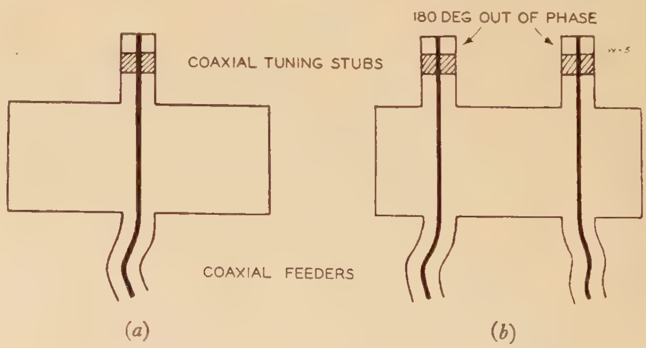

Figure 5. Methods of feeding rectangular guides

Here the transverse wire fed by a coaxial cable is set parallel to the electric field. In order to make adjustment for the purpose of maximizing the transfer of power, a coaxial tuning stub is added.

In order to set up the $TE_{0,2}$ mode, the arrangement shown in Figure 5*b* is used. In view of these two examples, methods of exciting any mode $TE_{0,n}$ are evident. On account of the similarity in configuration

between the rectangular $TE_{0,1}$ and the cylindrical $TE_{1,1}$ modes, a method of exciting the latter becomes evident also. However, the $TM_{0,1}$ mode is of more importance and the method used in its excitation is as shown in Figure 6. Here a length a of the inner conductor of a coaxial pair projects into the end of the wave guide and sets up electric flux lines as shown. The effectiveness of

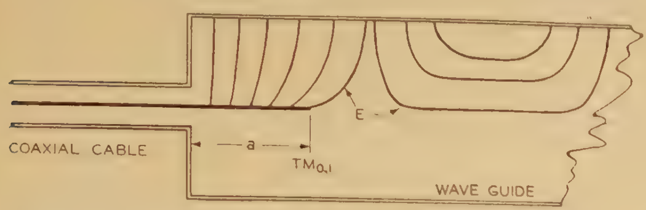

Figure 6. Method of feeding $TM_{0,1}$ mode in a cylindrical guide

the power transfer is dependent upon the length a which must be adjusted for any particular guide size and frequency.

Frequency Limits. Transmission will not occur unless the type of wave excited by the particular feeding method conforms to the cutoff-frequency requirements of the wave guide. As an illustration, assume that transmission through a tube having a circumference of 3π inches is required. The cutoff frequencies for various modes are as follows:

(*a*). $TE_{1,1}$ (cylindrical) 2,310 megacycles per second.

(*b*). $TM_{0,1}$ (cylindrical) 3,020 megacycles per second.

(*c*). $TE_{0,1}$ (square) 3,070 megacycles per second.

It is noted from the above that transmission of the $TM_{0,1}$ mode through a cylindrical tube requires a size more than sufficiently large for the $TE_{1,1}$ mode. This means that accidentally both modes may be present while transmitting the $TM_{0,1}$, which is a definite disadvantage in that calculations cannot easily be made if both waves are present. However, to offset this disadvantage, it is to be noted that the $TM_{0,1}$ mode is circularly symmetrical and for that reason detecting equipment placed at any point on the circumference of the guide will pick up signals equally well. The disadvantage noted previously, however, causes the use of this mode to be inadvisable except where necessitated by other considerations.

Turning now to the square guide, it is seen that its cutoff frequency is essentially the same as for the $TM_{0,1}$ mode. However, in a square guide, the cutoff frequency is determined only by the dimension perpendicular to the electric lines, and the other dimension may be made smaller without ill effects except through its influence on the characteristic impedance and attenuation. Also a square guide, such as that previously shown, may transmit the given mode in two positions, that is, due to some irregularity, the same mode with electric lines at 90 degrees to the original position may be set up. This is a distinct disadvantage because detecting equipment is usually connected to pick up only one polarization. The answer to this difficulty is to make the horizontal dimension, or dimension perpendicular to E, slightly greater than the critical value for the frequency desired, and the vertical dimension slightly less than this value. By this method then the $TE_{0,1}$ mode in one position only can be transmitted. Also since all other rectangular modes at this particular frequency require larger tubes, this is the only mode that can be transmitted.

Selection of Tube Size. As indicated, tube sizes should be somewhat greater than the critical or cutoff dimen-

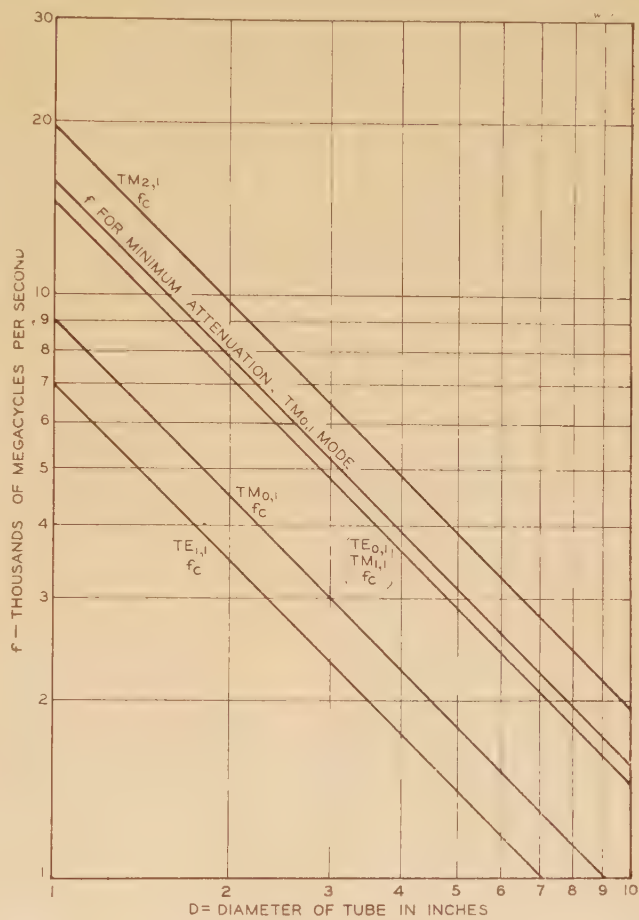

Figure 7. Cutoff frequencies for various modes of transmission

sion, and in the case of the rectangular guide, one dimension should be greater and the other less. For the cylindrical guide, the curves of Figure 7 are informative.

In order to decide definitely on a tube size, it is necessary to consider the effect of size on attenuation. For most modes, a minimum attenuation exists at some definite frequency, and this frequency varies for different tube sizes. It is advisable to select a tube size which will give a minimum attenuation at approximately the frequency to be used. The $TE_{1,1}$ wave has a lower

attenuation than the $TM_{0,1}$. However, for short lengths, this disadvantage is outweighed by the advantage, mentioned above, gained by the circular symmetry of the $TM_{0,1}$ mode. The relation for minimum attenuation in the $TM_{0,1}$ mode is $\lambda/b=1.5$. Actually, however, any value from 1 to 2 may be used as in this region the attenuation remains almost constant. For the $TM_{0,1}$ wave, the attenuation is almost flat over a 2,000-megacycle band.

As an illustrative problem, assume that it is required to determine a tube size to transmit 3,000 megacycles per second and to prevent the transmission of all modes above $TM_{0,1}$. The cutoff size is given by:

$$f_c=3{,}000\times10^6=3.76\times10^9\times\frac{2.405}{D\text{ inches}}$$

$$D=\frac{3.76\times10^9\times2.405}{3{,}000\times10^6}=3.02\text{ inches diameter}$$

Thus it is known that a tube larger than 3 inches is to be used. From the relation $\lambda/b=1.5$:

$$D=\frac{2c}{1.5(2.54)f}=\frac{6\times10^{10}}{(1.5)(2.54)f}$$

$$=\frac{15.75\times10^9}{F\times10^9}$$

$$=\frac{15.75}{3}=5.25\text{ inches}$$

where F is the frequency in thousands of megacycles per second. Thus for minimum attenuation, the diameter of the tube should be 5.25 inches. It is now necessary to determine whether any higher modes will be transmitted through this tube. The cutoff diameter for the next higher mode $TE_{0,1}$ is given by:

$$D=\frac{3.76\times10^9\times3.83}{3\times10^9}=4.8\text{ inches}$$

Accordingly the $TE_{0,1}$ mode would be transmitted and it is necessary to reduce the tube size to not more than 4.8 inches. Let it be reduced tentatively to 4.5 inches and recheck the size by the optimum attenuation condition.

$$\lambda/b=10/2.54\times2/4.5=1.75$$

This is a satisfactory size because λ/b falls in the range from 1 to 2. Table I and curves of Figure 7 are useful for calculations such as these.

Selection of Rectangular-Tube Dimensions. For transmission through rectangular tubes, it has already been pointed out that a form other than square should be used in order to maintain what may be called the original polarization. There is also a requirement on dimensions fixed by attenuation. It can be shown that, for various tubes of the same circumference but various values of a/b, a/b should be about 1.18 where a is the dimension parallel to the electric field. It will immediately be noted that these dimensions will allow the same frequency to be transmitted with the other polarization and thus the attenuation requirement cannot be used. Thus, in spite of an increase in attenuation a/b is made less than unity and may be made around one half in order to transmit as low a frequency as possible with as little copper as possible. The detailed considerations for size are somewhat involved, and in practical cases, since the transmission is generally for short distances, the primary determining factor may be convenience.

As an illustration, the attenuation for a copper tube 3 inches by 1.5 inches at 3×10^9 cycles per second is calculated to be about 5.0 decibels per 1,000 feet. This is, of course, far from the ideal conditions. The attenuation calculated for a tube with the optimum ratio of 1.18 with the same cutoff and transmitted frequencies is about 3.0 decibels per 1,000 feet. Note that here no attempt has been made to transmit the optimum frequency. That the attenuation can be very low for ideal conditions is seen from the fact that a tube of 40-centimeter circumference and the optimum a/b ratio at 4,000 megacycles per second is about 1.6 decibels per 1,000 feet.

Table I. Order of Modes for Calculating Tube Diameters

p	Mode (Cylindrical)
1.84	$TE_{1,1}$
2.405	$TM_{0,1}$
3.83	$TE_{0,1}$
3.83	$TM_{1,1}$
5.14	$TM_{2,1}$
5.33	$TE_{1,2}$
5.52	$TM_{0,}$

$f_c=3.76\times10^9\,p/D$ inches

D is the diameter of the tube in inches.

The tube considered above which has an attenuation of 5.0 decibels per 1,000 feet is designed to transmit 3,000 megacycles per second. It is interesting before leaving this part of the general discussion to determine how well this tube meets the cutoff requirement. The cutoff frequency for a rectangular guide is given by:

$$f_c=c/2b$$

$$f_c\text{ (for horizontal dimension)}=\frac{3\times10^{10}}{2(3)2.54}=1{,}970\text{ megacycles/second}$$

Thus certainly 3×10^9 cycles per second is passed easily. For the other dimension

$$f_c=\frac{3\times10^{10}}{2(1.5)2.54}=3.94\times10^9\text{ cycles/second}$$

Consequently this mode will not be transmitted with horizontal polarization. It remains only to show that the next higher mode $TE_{0,2}$ will not be transmitted.

$$\lambda=2b/2=c/f_c$$

$$f_c=\frac{c}{b}=\frac{3\times10^{10}}{3\times(2.54)}=3{,}940\text{ megacycles/second}$$

Since this is above the actual frequency being transmitted, the $TE_{0,2}$ mode will not cause any trouble.

Selection of $TE_{0,1}$ and $TM_{0,1}$ Modes. Sufficient material has been covered to justify the selection of the rectangular guide transmitting its lowest possible mode. For the purpose of ordinary transmission in a fixed

system, this method would be satisfactory. However, it suffers from the lack of mechanical flexibility in that it is difficult to transmit the $TE_{0,1}$ mode through elements which may change their orientation with respect to the source. For this reason it would seem that it would be better to transmit only symmetrical waves but as stated above, these permit lower modes which will interfere with measurements. A compromise must be made by using a symmetrical mode in movable sections and changing from one to the other whenever necessary. This requires, of course, an easy way of making the transfer from rectangular to cylindrical guides and vice versa. The measurements, being confined to the rectangular sections and thus, to the one mode of transmission $TE_{0,1}$, will show any irregularity in the cylindrical sections only through an indicated increase in the transition loss.

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An Electronic Circuit for Studying Hunting

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Various methods have been devised for determining power-angle oscillations of synchronous motors during the period of hunting following a change in load. This article describes an electronic circuit for this purpose, in which a beam of light is reflected from a set of equally spaced mirrors clamped around the motor shaft or coupling to a photoelectric cell; the light flashes reaching the photoelectric cell are amplified and impressed on the plates of a cathode-ray oscilloscope in such a way that the resulting beam pattern on the oscilloscope screen shows the power-angle oscillations directly.

WHEN a load is applied to a synchronous motor, there is a brief period of unstable operation in which the speed fluctuates slightly in order to allow the motor to adjust itself to the new operating condition. This interval of speed variation is known as the period of hunting. The problem of hunting is an important consideration in the design of synchronous machines, for such oscillations may often cause undesirable voltage and power fluctuations or the falling out of step of synchronous motors.

Although several methods have been developed for determining transient power-angle oscillations, they are all subject to certain restrictions and disadvantages. The new method proposed by the authors for the determination of the hunting characteristics of synchronous machinery was designed with the following in mind:

1. It should not involve any auxiliary apparatus that is difficult to obtain or set up.

2. It should be readily adaptable to equipment already in service and not operating under special conditions, such as in a laboratory.

3. The apparatus should be as simple as possible.

Suppose that a mirror is placed on the shaft of a synchronous machine and so arranged as to reflect a beam of light from a lamp into a photoelectric cell every time the rotor of the machine is in a particular position; then while the machine is running at synchronous speed, the photoelectric cell will receive flashes of light at equal time intervals. However, if hunting occurs, the time between successive flashes will vary: As the speed increases, the time decreases; conversely, as the speed decreases, the time increases. If a capacitor is arranged so as to begin charging at a constant rate at the same point on every wave of the potential applied across its terminals, and this capacitor is connected to the photoelectric cell in such a manner that every time the cell receives a flash of light, the capacitor is discharged, then

Essential substance of a paper "An Electronic Circuit for Determining Power-Angle Oscillations," which was awarded the AIEE national prize for Branch paper for the academic year July 1, 1940 to June 30, 1941. The paper was presented at the AIEE Southern District student convention, Tuscaloosa, Ala., April 4, 1941.

Manuel J. DeLerno and **Robert T. Basnett** were both students at The Tulane University of Louisiana, New Orleans, when the paper was presented; Mr. Basnett is now control engineer with the Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa. Mr. DeLerno is now development engineer in the industrial control department of the General Electric Company, Schenectady, N. Y.

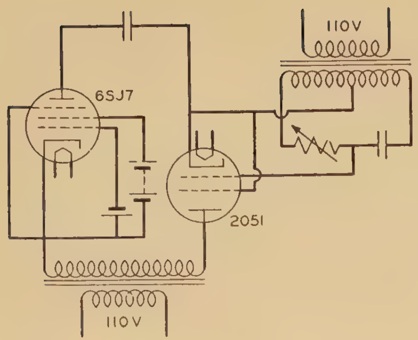


Figure 1. Schematic diagram of circuit for charging a capacitor at a constant rate

the charge on the capacitor varies as the length of time between flashes varies. For a given capacitance, the voltage is proportional to the final charge. Hence, the maximum voltage would follow the same fluctuations as the speed; that is, this voltage variation would be proportional to the hunting.

For the purpose of analysis the device that was evolved is considered as being made up of four distinct electrical and two mechanical units. The two mechanical aids consist of a lamp for producing a focused beam of light, and a metal band to which are attached several small mirrors. These mirrors are built into frames that are not rigidly fixed to the band, so that adjustments can be made before they are set permanently in position. The band itself is arranged so that it can be slipped onto the coupling of the machine under test and clamped. Because of the nature of the electrical components of the device, it is essential that there be as many mirrors as the machine has pairs of poles and that these mirrors be spaced approximately equidistant around the periphery of the coupling.

On the four-pole machine tested with the original apparatus, two such mirrors, about $^{3}/_{8}$ by $^{1}/_{2}$ inch in size, fastened to a 6-inch-diameter $^{5}/_{8}$-inch-wide 20-gauge brass band, gave very satisfactory results. Details relating to the construction of this unit and its alignment are discussed later.

The fundamental unit of the electrical system is a circuit for charging a capacitor at a constant rate. This is accomplished by connecting a vacuum pentode (*6SJ7*) in series with a gaseous tetrode (2051) and a capacitor. The circuit diagram for this part of the setup is shown in Figure 1. From the characteristic curves of this pentode, it can be seen that once the plate voltage has risen above a certain minimum value, the current through the tube is nearly independent of the plate voltage, and is, consequently, essentially constant in magnitude. To prevent conduction from occurring before the applied voltage is above this necessary minimum is the duty of the 2051 tube. By supplying this tube with a sufficiently negative grid voltage, conduction can be postponed the desired length of time, after which the circuit acts as if this gaseous tube were not present, for once breakdown has taken place the current is governed by the pentode only, until the applied voltage has dropped to such a value that it is equal to the capacitor voltage at which time conduction ceases. For the purpose of adjustment the grid voltage of this gaseous tube is provided by a phase shifter, which allows control over the time at which the tube breaks down and the capacitor charging current starts flowing.

Connected across the terminals of the capacitor in this charging circuit is a second gaseous tube (2051) the grid potential of which is governed by the second electrical unit of the apparatus—a gaseous photoelectric cell and its amplifier. This is illustrated in Figure 2. The photoelectric cell receives flashes of light from the mirrors fastened on the shaft of the synchronous machine, each flash being transformed into an electrical impulse which is transmitted through a single-stage resistance-capacitance-coupled amplifier to the grid of tube 2051. This grid has a bias of -20 volts, and, when it receives the sharp, positive pulse of about 90 volts from the amplifier, it becomes immediately conductive, and discharges the capacitor bringing the voltage of the capacitor to zero in a very short time. This statement assumes that the phase relation between the photoelectric-cell pulse and the capacitor voltage is such that the gaseous-discharge tube is made free to conduct at some particular instant after the capacitor voltage has begun to rise; this is the normal and desired operating condition. Since the charging circuit is rectifying in nature, it is obviously useless to have the discharge occur during the half cycle when no charging current is flowing, and consequently no voltage across the capacitor.

The third electrical unit required is a power supply. This needs no description for it is built along standard lines, being made of a type 80 tube and a filter section.

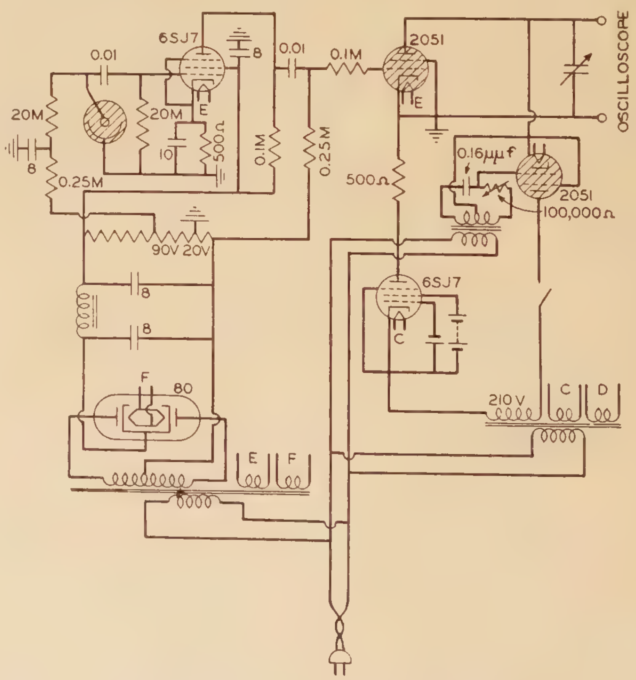


Figure 2. Schematic diagram of the gaseous photoelectric cell, its amplifier, and the charging circuit

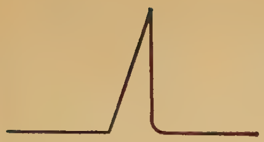


Figure 3. Form of wave recorded on the oscilloscope screen

The output is 300 volts and is used to supply the negative grid bias for the discharge tube and the plate voltages of the photoelectric cell and the 6*SJ7* tube in its amplifier. The three units mentioned thus far are built on one chassis, and constitute the special circuit required by this method. The fourth unit is a cathode-ray oscilloscope or a magnetic oscillograph, which are usually available as individual pieces of apparatus and need not be constructed integrally with the circuit. Either instrument may be used in this work, although the oscillograph requires a special coupling circuit. However, the oscillograph has the advantage of being able to show several quantities on one film. The oscilloscope is somewhat easier to use, although it requires that a photographic record be taken, for the hunting wave obtained is not a periodically repeating wave such as a wave of current or voltage in a stable circuit. The results described herein were obtained with a cathode-ray oscilloscope and the remaining discussion relates to its use, but it should be remembered that the magnetic oscillograph would serve equally as well.

The vertically deflecting plates of the oscilloscope are connected to the terminals of the capacitor in the charging circuit. If then the proper sweep frequency is chosen, and the synchronous machine is operated at constant load so that no hunting occurs, a triangular wave appears on the screen of the instrument. Figure 3 shows the form of this wave. How it originates is as follows: Current begins to flow in the charging circuit at some instant regulated by the phase shifter. Because this current is constant in magnitude, the charge across the capacitor increases at a constant rate, and the capacitor voltage rises accordingly. Before the capacitor has completed its charge, however, it is discharged by the photoelectric cell, and its potential drops to zero until the next cycle. When the load on the machine is changed, and equilibrium has been re-established, the length of time that elapses between the beginning of the charge and the discharge pulse is likewise changed, due to the shift made by the rotor. This causes an increase or decrease in the height of the triangular wave that is proportional to the change in power angle of the machine. Strictly speaking, the variation in altitude is proportional to the shift in phase between the rotor and the applied voltage, but this is close enough to be considered as equal to the shift in power angle.

During the period of instability or hunting, the amplitude of the triangular wave is constantly varying. This variation can be seen on the screen, but is of little value, since one deflection falls right on top of the preceding one and the eye is not quick enough to follow the cycle of change. To make a record of the hunting cycle, a very slow, nonrepeating sweep must be used on the oscilloscope. The internal sweep circuit of the average instrument is not suitable for this purpose, and an external sweep must be used. The voltage across a capacitor that is charged through a resistance from a constant-potential source gives a fairly linear horizontal sweep for the interval of time involved, but a more elaborate and accurate one can be constructed in the same manner as the internal sweep. If this sweep is started at the time the load on the machine is changed, the triangular waves will be traced in sequence, one after another, by a moving spot. By making a time exposure with a camera—keeping the shutter open until the sweep is completed—a complete set of waves is obtained, the tips of which form the locus of the hunting wave. The oscillogram reproduced in Figure 4 was taken using a sweep circuit consisting of a four-microfarad capacitor and a one-megohm resistor in series with a 45-volt battery. In obtaining this oscillogram, the switch used to load the synchronous motor also closed the sweep circuit, ensuring proper co-ordination. One feature of these results is that no timing wave of any kind need be obtained, for the start of any one triangular wave is $^1/_{60}$ of a second behind the corresponding origin of the preceding one, providing that the voltage to the circuit remains constant. This supplies a convenient time axis.

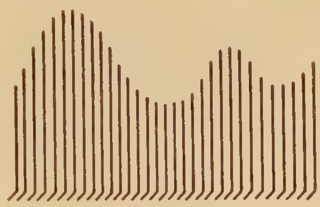


Figure 4. Reproduction of a typical power-angle oscillogram taken on a four-pole synchronous motor

Because the operation of this apparatus involves time, the sequence in which different parts of the circuit function is a prime consideration. For instance, if the pulse from the photoelectric cell is received after the charging current has ceased flowing, the voltage wave projected on the oscilloscope screen has a flat, horizontal top, because there is a maximum voltage to which the capacitor can charge, depending on its capacitance, the charging current, and the applied potential. This flat portion may be eliminated by delaying the start of the charging by means of the phase shifter in the first gaseous tube.

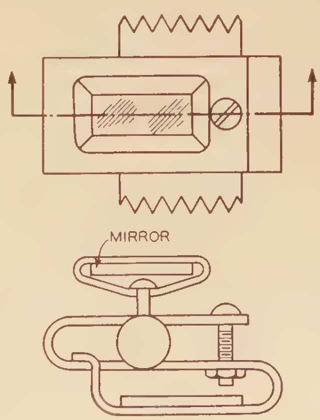


Figure 5. Diagram of mirror attachment

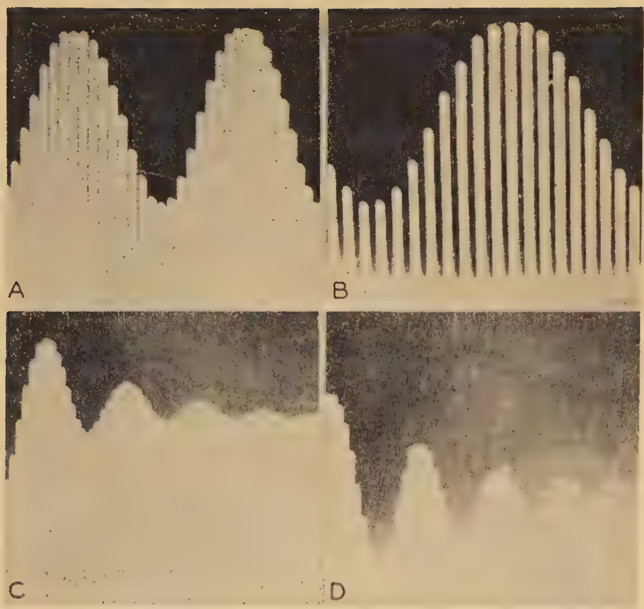


Figure 6. Typical oscillograms taken by the method described in this article

A. Double-current generator operating as synchronous converter (no amortisseur winding)

B. Same as A except with different oscillographic sweep

C. Full load thrown on a synchronous motor

D. Full load removed from a synchronous motor

However, if the flat portion is nearly a half cycle in length, the terminals of the 110-volt lead from the supply to the equipment should be reversed, for this trouble means that the discharge tube is receiving its photoelectric-cell pulse 180 degrees too late. Correction by use of the phase shifter cannot always be applied. If charging is delayed too long, the maximum voltage obtainable, and consequently the deflection on the fluorescent screen, is considerably reduced, for the capacitor voltage becomes equal to the applied potential at some point long after that potential has passed its maximum. If such is the case, all of the mirrors must be shifted an equal amount in the direction of rotation. One other trouble arises when the machine is reconnected after a period of idleness. If the d-c field is not connected with the same polarity it had during the previous test, the machine will pull into step 180 electrical degrees from its original position. When this happens, the field should be reversed or the mirrors shifted a corresponding amount.

The design of the mirror attachments is an important item. The type built for the apparatus described here is diagrammed in Figure 5. The mirrors are mounted on universal joints held in closed S frames equipped with friction clamps. All that is necessary to test synchronous machines of any number of poles is to have a supply of these mirrors available. The only thing that needs individual fitting is the band itself which must be cut so as to encircle the particular coupling on the rotor. Before attaching the band, as many mirrors must be slipped onto it as the machine has pairs of poles. They are placed approximately equidistant around its periphery, and each is held in place with a spot of solder. One mirror is chosen as reference (this mirror need not have a universal joint). This reference mirror is placed in any convenient position; then the photoelectric cell is arranged so as to catch the reflected beam of light from the lamp. The position of the rotor should be observed and marked. Next, the rotor is revolved 360 electrical degrees, so that the 360-degree mark coincides with the fixed reference. By adjusting the second mirror on its universal joint, it can be made to catch a beam focused on it by the lamp and reflect it into the photoelectric cell. It is then clamped with the adjusting screw. The process is repeated for each of the remaining mirrors. This system, therefore, does not require that the mirrors be spaced exactly right, but that the photoelectric cell receive a beam of light 360 electrical degrees after the preceding flash. With reasonable care and a sharp focus, this can be accomplished. If the flashes are not 360 electrical degrees apart, triangular waves having equal altitudes will not be obtained even when the machine is not hunting, and the results will be useless.

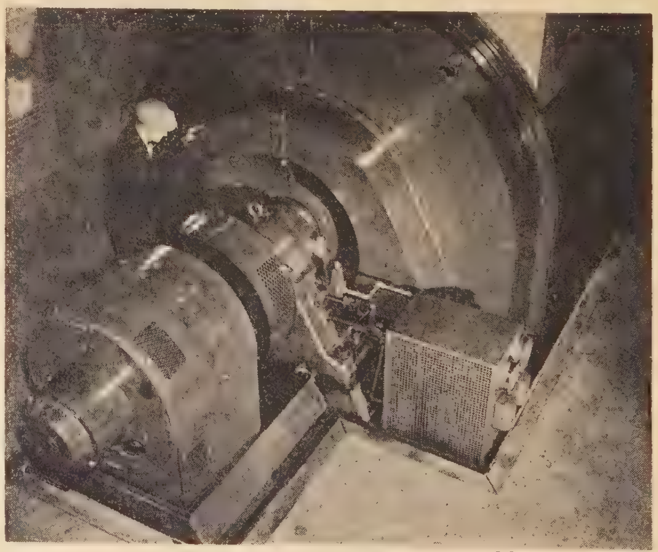
This circuit seems to be an improvement in several respects over other devices for determining power-angle oscillations. These may be summarized as follows:

1. The method is applicable to synchronous motors having any number of poles, without additional equipment.

2. The normal operating characteristics are not influenced to an appreciable extent by the apparatus.

3. The end of the motor shaft need not be available. The only room necessary is enough to mount the mirrors on the shaft.

4. This apparatus is equally suitable to measure constant power angles.



Westinghouse photo

Exciter end of a 15,000-kva synchronous condenser

More Mica From the Western Hemisphere

Mica, which heads the list of critical raw materials, is needed so urgently that quantities are being flown to the United States from India. Action is being taken now toward developing domestic mica resources, chiefly in North Carolina, Georgia, New Hampshire, Connecticut, and South Dakota. In this article mica sources, especially those in the Western Hemisphere, are surveyed and the production problem is found to be based on the dearth of skilled, low-cost labor.

WITH war-caused shifts in world trade has come an increasing interest in sources of mica in the Western Hemisphere. Argentina, Brazil, Mexico, Canada, and the United States all have rich deposits of the mineral. These deposits are being surveyed with a view to increasing present production and commerce.

India is by far the largest producer of mica. Out of a total world production of sheet and splittings totaling approximately 9,016 short tons in 1938, India supplied 6,334, Madagascar 747, the United States 469, Canada 68, Argentina 258, and Brazil 575.

In the other American Republics, production of mica for strategic uses has been almost entirely confined to sheet mica. Previously the mineral was exported chiefly to Germany, to India (for splitting) and, in small quantities, to the United States. In recent years, production has increased very considerably both in Brazil and in Argentina. Brazilian exports rose from 49 short tons in 1929 to 575 in 1938 and 1,229 tons in 1940. After the outbreak of war in Europe, there was an increase in exports to Japan, the United States, Great Britain, and India. Argentine exports rose from 58 short tons in 1929 to 248 in 1938.

Thus both Brazil and Argentina have been small but increasing producers of mica. As yet they supply only sheet mica. In the case of sheet mica, the quality of the deposits is the chief factor in determining where to produce; in the case of mica splittings (mica split into thin films, later to be joined together to make large sheets of mica), labor costs are the determining factor. Deposits of mica in Brazil, Argentina, Peru, and Mexico could be developed considerably for crude mica. Likewise, production of amber mica in Canada can be expanded if prices rise. If the Western Hemisphere should become dependent upon its own resources, even with increasing prices and production there would, in all probability, be available a large part of mica requirements.

The problem of production in the Western Hemisphere is chiefly one of finding cheap and efficient labor to grade and split the crude material. Consumers of mica have become accustomed to the excellent grading developed in India from long years of experience. The expansion of sheet production in Brazil and Argentina has been hampered by the fact that standards of classification have not been carefully followed. Consequently, purchasers in this country have preferred to pay higher prices for Indian sheet mica, because they can rely upon deliveries to be exactly as specified. Thus, if sales of sheet are to be improved in Brazil, it is essential that a careful system of grading be established.

Various plans have been suggested for introducing the mica-splitting industry into the Western Hemisphere. Canada already produces a small quantity of splittings and could produce more if prices rose. Small quantities of splittings have been made also in the United States, and in Mexico. Puerto Rico has been suggested as a possible center for mica splittings on account of its abundant supply of cheap labor. Mexico also might increase its splitting industry. Even in these two areas, however, prevailing wages are very much higher than those in India.

HISTORY

Since the time when the first mine was dug in New Hampshire in 1803 to obtain mica for stove windows and lantern chimneys, the uses of mica have become much more varied. With advances in the field of electricity, mica has become an essential material in the manufacture of electric machinery and appliances. The invention of built-up mica in 1892 made possible many of the modern uses for the mineral. Built-up mica is made up of a number of thin films of mica held together by some insulating cement, usually shellac. Such sheets can be made in any size, and then cut down for specific uses. Through the development of built-up mica, it was possible to adopt standard designs for generators and motors. It was also possible to use many small pieces of mica to build up a sheet of much larger size than could be found frequently in natural mica formations.

The property which makes mica indispensable in electrical work is that it can be divided into thin, flexible, transparent films, which are unaffected by fire, water, electricity, or acid, and whose volume remains constant in extreme heat and cold. These characteristics are found in no other substance, and no synthetic substitute having similar qualities has been discovered so far.

MINING

The best quality mica is found in regions which have been relatively free of earthquake and volcanic dis-

This is an excerpt from an article, "Strategic Mica," published in the March 14, 1942, issue of *Foreign Commerce Weekly*.

turbances. The most important among these are the Indian Peninsula, Central and South Africa, and the eastern parts of the Western Hemisphere. Geologically, mica occurs in two forms, namely muscovite or white potash mica, and phlogopite, or amber magnesia mica. Mica found in India, the United States, and Brazil is of the muscovite variety, while that mined in Madagascar, Mexico, and Canada is phlogopite.

Mining of mica is made difficult by the fact that mica does not occur in any specific part of the pegmatite dike, the rock formation in which it may be found. The dike itself is usually less than 300 feet in length, comparatively narrow, but of variable depth. Diamond drilling which explores only a small area is not practical, because it might miss the mica entirely. In addition, even when mica is found, it may prove to be of inferior quality and suitable only for grinding. Mica mining is usually carried on with little equipment, since it is impossible to determine how extensive a deposit may be in any given area. Furthermore, even when large deposits are found, the return is comparatively small.

Accordingly, mica mining in the United States is usually conducted on the principle that the work must pay for itself as it proceeds, and substantial capital expenditure is rarely made. In many cases, mica mines which have been deserted when a streak of mica has disappeared, have been reopened later by more hopeful prospectors, or on account of a rise in the price of the product. This is true of mines in India and Canada as well as in the United States.

Many of the chief problems of mica production, particularly in the production of strategic types of mica, are concerned with preparation and sorting. When the chief use of mica was for stove windows, large sheets only were usable. Today there are uses for many smaller sizes. In addition, the development of micanite has increased considerably the utility of small blocks of mica.

Only a very small percentage of the total available mica is suitable for block mica or mica splittings. Although the percentage varies in different localities, usually 70 to 85 per cent of the output of a mine is saleable only as scrap. A further percentage is suitable for punch mica, and about 10 per cent for sheet mica. Of the knife-trimmed sheet, 50 to 70 per cent will be the smallest acceptable size, $1^1/_2$ by 2 inches, rarely more than 10 to 13 per cent will be as large as 3 by 4 inches; and it is always possible that a large part of these sheets will be stained, and thus unsuitable for all uses.

CLASSIFICATION

In appraising the mica deposits, the most important fact is that the substance is found in a variety of different sizes and states of purity, each of which is of a different degree of utility. For many strategic uses, mica must be of certain minimum dimensions, and must also be without stains, which are an indication of the presence of another material, such as iron, not having the same physical characteristics as mica.

For commercial purposes, mica is classified in four categories according to size. Scrap or waste mica is so irregular in size or formation that it can be used only for grinding. Punch mica is sheet mica, usually less than $1^1/_2$ by 2 inches in size. Splittings are sheet mica usually less than three quarters of a square inch in area, and 0.001 to 0.0012 inch in thickness. Splittings mainly come from material too small for use as sheet or block mica, which is the next category, usually ranging in size from $1^1/_2$ by 2 inches to 8 by 10 inches in area, and not less than 0.01 inch in thickness. In the United States, mica sheets are classified according to the size of the largest standard rectangle that can be cut from the sheet.

From the standpoint of strategic importance, only mica sheets or blocks and splittings are regarded as of value, and thus, in considering mica production in relation to war production, the only figures of value are those which distinguish the output of the different types of mica. Not only is mica graded by size, but also by quality, distinction being drawn between clear, slightly stained or spotted, and heavily stained or spotted mica.

Scrap Mica. Waste and scrap mica, which are used for roofing paper, are of value only in the United States, because only there has the process of grinding become adequately developed. A considerable amount of waste is imported for grinding, and some of the ground product is exported.

Block or Sheet Mica. In production of block mica, the preparation and grading are of the greatest importance. Because of the presence of highly trained labor and low wage rates, India has become the chief source both for block mica and for mica splittings. To prepare mica as sheet material, the rough pieces are split into thicknesses from 0.01 to 0.10 of an inch thick, and are then trimmed of imperfections. The sheets are then graded according to the largest rectangle of specific dimensions which can be cut from them. Sheets thus graded as to size are sorted then according to quality. Men who do this work are highly paid, as they can often eliminate imperfections by resplitting a sheet, thus considerably improving the quality and value of the mica.

Splittings. The most important form in which mica is used is as mica splittings. As stated above, India is by far the most important source of splittings, because of favorable labor conditions. There, many small "books" (the term for mica crystals, somewhat like books in shape) which in other areas would be discarded, are used for making splittings. Many such "books" have been recovered in India from waste dumps which accumulated through the centuries before the discovery of the manufacture of micanite, when the demand was exclusively for large sheets of mica. Mica may be split to a thickness of one one-thousandth of an inch. The work is done largely in homes by women and children

working at very low wages—from 9 to 12 cents a day. Amber mica, like white mica, is split by hand. For many years Canada produced a considerable quantity of amber mica splittings, but when the amber mica deposits of Madagascar were developed, the cheap labor available there enabled producers to market amber mica at prices with which Canadian producers were unable to compete.

A number of attempts have been made to produce a satisfactory machine for splitting mica. One for splitting amber mica was relatively successful, and was used commercially, but was finally abandoned because of its limitations in producing splittings of required thickness. Many mica experts are of the opinion that there never will be a really satisfactory machine for splitting mica, particularly for muscovite, the splitting of which is more difficult than that of the amber variety.

Fortunately, large supplies of mica are found in the Western Hemisphere. The United States and Canada have for some years been sources for mica. The crucial problem in production, should the hemisphere be separated from Indian sources, lies not so much in lack of the raw material as in the lack of trained, cheap labor to grade and split the material.

Switchgear Practice in Europe and America

J. B. MacNEILL
MEMBER AIEE

America and Europe are developing different types of switchgear equipment, conditioned by the type of facilities available in certain regions and the particular requirements in those regions. Present trends in switchgear apparatus are guided to a considerable extent by the abundance or scarcity of the material resources involved in construction and operation of the equipment.

DURING the past several years, there has been a major change in the handling of switchgear problems—the strong tendency toward completely factory-assembled and tested equipment. American switchgear was slow to adopt this type, which got its start in Great Britain. The present American practice, however, justifies itself by providing at a reasonable cost the adequate interrupting capacity, ease of inspection and maintenance, and freedom from trouble which are essential to our use.

Since metal-clad assemblies in America have been more recently developed than in England, they have incorporated improvements in circuit-breaker design which have resulted in simplification of the complete equipments without loss of safety. It has not been necessary to the same extent to provide for defective switch operation.

Because there have been in the past ten years major differences between our circuit-breaker construction and those of continental Europe, the subject has had a great deal of discussion. At times we have seemed backward in adopting new forms of interrupter having a minimum of oil or no oil at all, which our European cousins have told us were adequate for their service. However, there are differences between our situation and that in Europe.

1. Our rupturing capacities both at high and low voltage are greater than those generally used in Europe.

2. Reclosing duty cycles have been standard with us for 15 years and have needed more adequate interrupting devices with better assurances of trouble-proof operation and reduced maintenance.

We have sold in the past few years power-station circuit breakers for 2,500,000-kva capacity at 13 kv. The same capacity is frequently sold at 132 kv and 230 kv, and there have been discussions of 3,500,000 kva at the higher voltages. Rugged mechanical devices typical of American switchgear are necessary for these extreme requirements. Nevertheless, for approximately 15 years in America there has been a continuous search on the part of several manufacturers for new interrupting ideas that might be incorporated in switchgear when these should appear feasible. The Westinghouse company developed the "De-ion" breaker experimentally for voltages up to 15 kv in the years 1925 to 1927. It was tested on the Commonwealth Edison System in Chicago to over 500,000 kva at 12 kv in 1928, and a large number were installed on the same system in 1930–31. Later experimental developments

Essential substance of an address before the AIEE Toronto Section, March 13, 1942.

J. B. MacNeill is manager of engineering, switchgear division, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

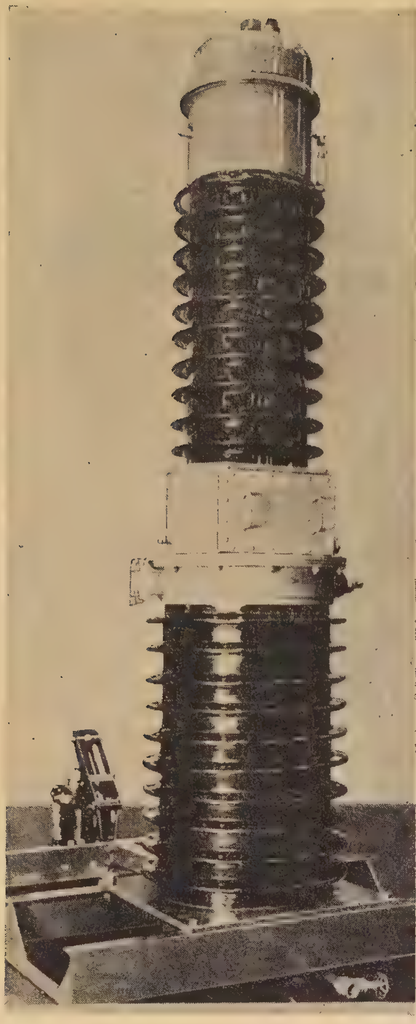
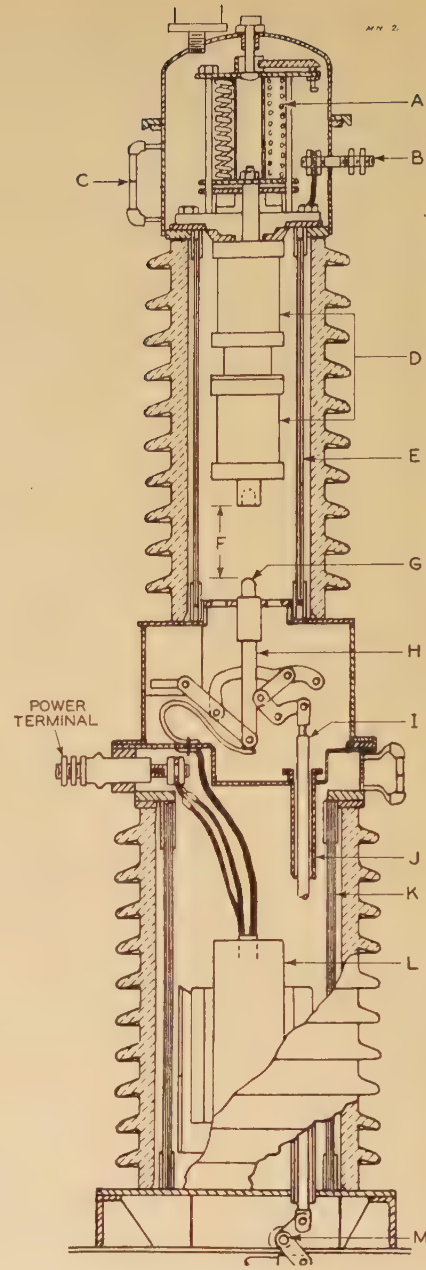


Figure 1 (left). Single-pole 138-kv 1,500,000-kva vertical-flow oil-poor circuit breaker

Figure 2 (right). Schematic diagram of the single-pole breaker

A—Opening accelerating springs
B—Power terminal
C—Oil level
D—Interrupters
E—Insulating supporting tube
F—Oil disconnect gap between terminals—breaker open
G—Disconnect member
H—Pole-unit mechanism
I—Insulating operating rod for both opening and closing
J—Insulating tube surrounding operating rod
K—Insulating supporting tube
L—Oil-insulated current transformer
M—Torsion shaft



have covered vacuum breakers, boric-acid breakers, oil-poor breakers and compressed-air breakers.

To get a better view of American switchgear requirements, the field is divided into three classes:

1. Power-station auxiliary and small substation work 15 kv and below, 250,000-kva interrupting capacity and below (generally indoor).

2. Power-station and large substation main switchgear 23,000 volts and below, interrupting capacities up to 2,500,000 kva.

3. Outdoor high-voltage requirements 23 kv to 287 kv rupturing capacities up to 2,500,000 kva.

A proper understanding of the present situation and the changes that have taken place will be reached if we consider each class by itself. These class lines, of course, cannot be considered definite, because the classes mix to some extent, but for our purpose they are adequate.

POWER-STATION AUXILIARY AND SMALL SUBSTATION EQUIPMENT

Present American Practice. While in the early days of electrical engineering various air-type devices were used in the field of power-station auxiliary and small substation equipment, oil circuit breakers have since superseded them for a-c work. Numerous oil breaker designs both oiltight and nonoiltight are available in common tank and multiple single-tank constructions. The maximum savings in space and cost are generally found by the use of common tank devices. While the single-pole form was preferred years ago because of phase isolation, the improvements in design have made the more compact structure quite possible up to 250,000 and even 500,000 kva. The recognition of this type for high-power work resulted from the Detroit tests of 1924 in which the main 4,800-volt bus of the Delray station was short-circuited through a three-pole breaker having a 20-inch tank. With this type it was possible to modernize many outgrown equipments within existing space and with freedom from physical demonstration and oil throwing.

European Developments. In Europe, particularly Germany and France, several devices have been developed in an attempt to supplant the oil breaker. Of course carbon breakers and contactors fill the 600-volt field most frequently there as in America. However, above 600 volts compressed air, self-generated air blasts, chemical resins, and magnetic types are being used.

Recent American Developments. In the United States, practically all commercial developments of the De-ion type of gear have been for self-contained units incorporating magnetic blowout effects in one form or another. One large manufacturer features Magne-Blast in which a powerful magnetic field drives an arc between converging barriers so that the power factor of the arc is increased before rupture. A second manufacturer sells a magnetic type up to 5,000 volts featuring double blowout coils inserted by the arc action as it moves off the main contacts, and these coils lengthen and cool the arc until it becomes unstable. The "De-ion" breaker development is available throughout this class but has a current-interrupting limitation which prevents its use in the largest capacities. A modified form of "De-ion" breaker called "*DH*," at 2,500 and 5,000 volts, provides interrupting with a short total arc length and covers the field up to 250,000 kva at five kilovolts, with prospects of going higher.

POWER-STATION AND LARGE SUBSTATION MAIN SWITCHGEAR

Present American Practice. Since approximately 1895 the oil circuit breaker has practically without exception handled power-station and large substation switchgear in America. The rupturing capacity requirements are

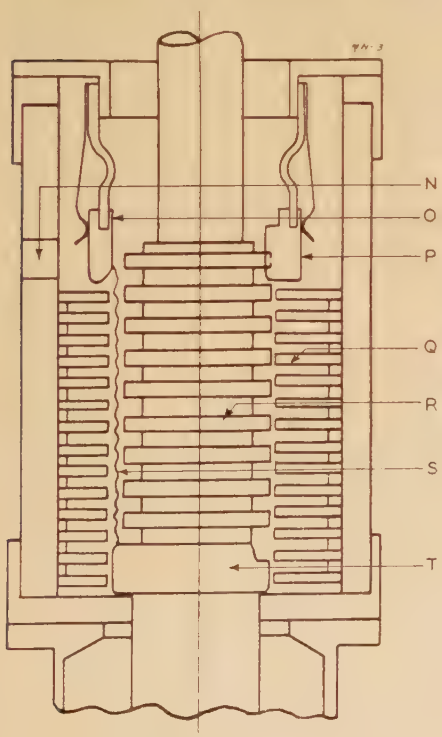


Figure 3. Schematic diagram of the upper unit of the vertical-flow interrupting element of the single-pole breaker

N—Vent
O—Stationary arcing contact
P—Stationary main contact
Q—Stationary corrugated wall
R—Moving corrugated follower
S—Arc
T—Moving contact

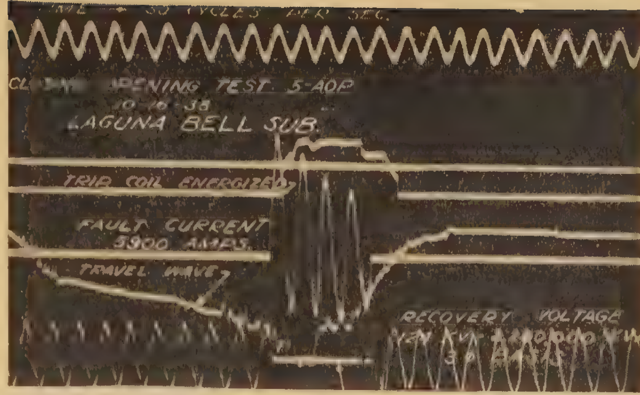


Figure 4. Oscillogram showing closing-opening operation of the 138-kv vertical-flow breaker on a 220-kv system tested line to ground

Current interrupted 5,900 amperes; three-phase power equivalent to 2,200,000 kva

great, 1,500,000 kva being a frequent requirement and 2,500,000 kva occasionally called for. The dead-tank circuit breaker is made by all manufacturers and the live tank form by one. In the early years of the development concrete and brick cell work was generally used to limit the effects of distress operations on the breakers. More recently, however, the integrity of the breaker itself has improved so that it has been possible to confine oil and gas to the interior of the structure with proper venting arrangements for hydrogen fumes and with complete absence of flame external to the breaker. Tests in high-power laboratories and further tests on large operating systems have made this development possible.

Reclosing service in this class is not generally used. The main problem is to withstand the heavy short-circuit stresses and rupture the short circuit satisfactorily a minimum number of times. Obviously frequent fault operation on heavy station switches would not represent a practical continuity of service and would need to be corrected without delay. However, it has been found that a switch which will adequately handle a short circuit once will continue to do so any reasonable number of times. This is true, because the contact and oil depreciation with modern interrupters is reduced to negligible amounts and the serviceability of the switch thereby increased.

European Practice. About 12 years ago the European countries faced a problem similar to ours—of greatly increasing the ability of their switchgear along existing lines or departing radically from the older forms. England has kept the dead-tank form, because it readily adapted itself to metal-clad gear. On the other hand, Germany and France, because of shortage of oil and other materials, and because their development of standard breakers had lagged somewhat, decided on new forms. One of these is the expansion breaker sold for 800,000 kva and below at generator voltages with water as a dielectric. Another form is the compressed air

breaker, sold for 800,000 kva at generator voltages. Both these forms were developed originally in Germany and have spread to a considerable extent to other continental countries. Each of them, however, has reached approximately the same limitation on kilovolt-amperes as the "De-ion" breaker has in this country for generator service, about 800,000 kva, and this has prevented serious consideration of the European types with us until recently.

Recent American Development. In the past two or three years there has been added pressure of American manufacturers to provide oilless breakers for station service. American manufacturers are prepared to furnish to a greater or lesser degree compressed-air breakers for this service. These have actually been contracted for in sizes 250,000 kva up to 2,500,000 kva for 15 kv and below. This is a new type, and its general construction may be of interest:

1. The grounded metal parts including air tanks, control valves, operating piston, and auxiliaries are all mounted in a subchamber. Inspection and maintenance can be taken care of through the lower doors without entering the high-voltage chamber.

2. Air is taken from the tank in a straight line through the connecting pipe to the arcing chamber and thence through the mufflers to the exhaust spaces in the top of the cubicle. There are no changes of direction in the air, and the loss of air efficiency is eliminated.

3. The contact structure and operating piston are permanently tied together at all times, both closing and opening being secured by reversal of the main piston, but without the necessity of latches and accelerating springs.

4. The full automatic trip-free function is obtained by quick reversal of the air in the operating cylinder and by immediate opening of the dump valve on the closing side. In this way high-speed opening and closing are both obtained with a minimum number of parts, and the mechanism is always in a position to respond to air pressure.

5. Liberal muffler structures are provided on top of the arc chute to eliminate flame and reduce noise.

6. Since the blast valve is mechanically operated from the mechanism, it need not be opened when the breaker is closing, and there is thus a saving of compressed air. Also, the mechanical operation of these valves insures both positive action and that the opening is of a minimum duration. Experience with magnetically operated valves for this purpose shows that they are uncertain in timing and use unnecessary air.

The compressed-air system for use with such breakers is reasonably simple, consisting of a two-stage slow-speed compressor unit mounted on a small tank with a larger storage tank alongside. Cooling coils prevent moisture getting into the ultimate breaker supply, shutoff valves prevent loss of air in the individual breaker tanks in case of supply failure. In important stations several of these compressed-air units will be used, each unit supplying a group of breakers, but the several air systems tied together to afford reserves on each other. At the present time it looks as though water breakers, boric-acid breakers, vacuum breakers, and other experimental substitutes for oil breakers would not be commercialized on a large scale in America. The compressed-air form seems a more logical companion to the small dry breakers previously discussed.

OUTDOOR HIGH-VOLTAGE EQUIPMENTS

Present American Practice. Actual operating voltages in America outdoors cover the range 2,300 up to 287,000 volts, and the old rule of 1,000 volts per mile for transmission length still represents good economy. The outdoor-switchgear field is a very large and important one. It is here that the volumes of oil are greatest because of the insulation spacings required in the apparatus, and therefore one looks naturally for attempts to eliminate the oil circuit breaker. It is a fact, however, that the standard dead-tank oil circuit breaker is well thought of in this country and has been highly developed through co-operative field tests and factory development tests, so that it is a highly reliable unit capable of heavy interruptions, high-speed operation, reclosing service, and freedom from trouble during all weather conditions without the necessity of auxiliary devices to disconnect it from the circuit after interruption. Many of the devices suggested for substitution do not possess all the operating advantages of the standard oil breaker, and this country is loathe to adopt schemes which sacrifice system operation even to a slight degree.

However, the amount of business in the outdoor high-voltage field is so large that manufacturers naturally have investigated any probable substitutes for their present equipment.

An interesting auxiliary development in connection with large outdoor breakers is the use of compressed air for operation. It has been found practical with such a unit to secure faster synchronizing and reclosing time than with magnets or motors on very large sizes. The individual compressor and storage tank at each breaker does away with piping problems from central storage systems.

European Practice. Again because of scarcity of oil for electrical purposes, government regulations on its use, and the cost of metals as compared to porcelains, continental Europe has taken the lead in oil-poor and oilless devices for high-voltage service. England again has retained the practice of dead-tank circuit breakers generally. Automatic reclosing is not generally necessary on the continent because of the use of ungrounded systems with Petersen coils. The principal requirement of an expansion-type oil-poor circuit breaker is ability to interrupt line-to-line short circuits infrequently. Line-to-ground short circuits do not generally call for switch operation.

Another form generally used in Europe is the compressed-air breaker. It is a form arrived at after several years experimentation to eliminate moisture and creepage troubles in the outdoor application of compressed air. It has been in operation as a type from four to five

years, but it can hardly be said that there is sufficient background of operation experience to state that it is trouble-free.

Recent American Development. At the present time there is an honest difference of opinion among engineers as to the ultimate development for high-voltage outdoor equipment, assuming that a change from the present dead-tank breaker takes place. Two types are favored, the oil-poor and the compressed-air.

An appreciation of the value of oil as an insulating medium has led some American manufacturers to favor an oil-poor device.

Another form is the Westinghouse vertical flow which, while not in commercial use in America, has been largely produced in Italy and to a less extent in other European countries. It was developed here and was tested on a 220-kv system in the West up to 2,250,000 kva. It has decided advantage for American use, including high-speed reclosure, no external disconnecting devices, high-speed circuit rupture, minimum floor space and oil content. The general satisfaction with standard dead-tank oil breakers has resulted in withholding of manufacture of this type with us.

Compressed air for high-voltage outdoor use is engaging the attention of several manufacturers. A few installations are being made. Compressed air has certain natural advantages for this use, including high-speed interruption, high-speed reclosing and the prompt opening of charging currents and magnetizing currents. Its problems are those of insulation and isolation.

One type of compressed-air outdoor breaker is being produced by the Westinghouse company for field trial installations. With this type it is possible to handle 138 kv in a single interrupter which has been tested up to 2,500,000 kva on a three-pole basis. This interrupter is a composite design using certain features of the longitudinal blast and transverse blast which we feel accounts for its great interrupting capacity. An interesting part of this development is associated with a cold room in which temperatures down to minus 20 degrees Fahrenheit can be secured for proving the operating mechanism and disconnect features under winter conditions. Arcing times are uniformly within one cycle, and arc energy is remarkably low.

Trial installations of 230 kv in this type of compressed air are under consideration. For interrupters it uses two in series, each essentially the same as the 138-kv interrupter referred to above. While five cycles will probably be the industry standard operating time, there will be a demand for three cycles, and compressed air seems to provide for this demand with a minimum of difficulty.

HIGH-POWER LABORATORY

Mention has been made several times of very high values of power for test purposes. These tests have been made possible through the increased laboratory at East Pittsburgh wherein are arranged two machines for parallel operation, each giving approximately 1,000,000-kva symmetrical first-cycle three-phase short circuit. The output then at bus voltage of this laboratory is 2,000,000 kva symmetrical with correspondingly greater amounts of asymmetrical currents. Transformers are provided up to 345,000 volts three-phase and 390,000 volts single-phase. The whole equipment is described in an AIEE paper and provides the largest concentration of laboratory test power available. The use of this equipment is of the greatest value in determining design features and demonstrating complete apparatus. Over 100,000 tests have been made since the original machines were first installed..

REFERENCE

1. High-Capacity Circuit-Breaker Testing Station, **J. B. MacNeill, W. B. Batten.** AIEE *Transactions,* volume 61, 1942, February section, pages 49–53.

Induction Heating Saves Tin

The present tin shortage has accelerated the development by the Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., of a processing line for the electrolytic deposition of tin on steel; high-frequency induction heating speeds the output of tinplate and produces a smooth, shiny, corrosion-resisting finish by causing the coating of tin to reflow. One of the chief advantages claimed for the process is that it uses only one-third as much tin as the method of dipping steel sheets into tanks of molten tin.

After passing through the plating tanks, the tinned steel sheet goes into the inductor heater coil where it is heated to the fusion point of the tin. As the sheet moves through the coil, the minute peaks of tin are melted into an even finish. The melting action stops when the strip moves out of the coil and water cools the metal. High-power vacuum tubes supply high-frequency (200,000 cycles) energy to the inductor.

Several advantages are claimed for the induction heating method of finishing tinplate over the gas-furnace method. First, it avoids cumbersone furnaces, which must be considerably longer than the induction heating coil, and in addition it necessitates no supporting rolls and thus eliminates the possibility of marring the surface. Further, when the end of the coil of the steel strip approaches the inductor heating coil the operation must be slowed to weld on another strip. The heat generated can be adjusted to compensate for the loss of speed, while in a furnace this cannot be done. An additional advantage of the induction heating method is that its high-frequency unit can be put directly into the electrolytic tinning line and can operate as fast as the strip can be plated.

I Am an American

ARAM BOYAJIAN
FELLOW AIEE

I LOOK back over half a century to refresh my memory of what being an American has meant to me progressively through all these years. Born in Armenia, I derived my earliest conception of America from story tellers, and it was about as fantastic as Alice's Wonderland. At the age of 21, while teaching arithmetic in a school, the thought occurred to me one winter morning that I should go to this romantic country. Within a few hours I was on the way. What? Such haste? Doesn't one wait to the end of the school year? Of course not; that is, not if one has indulged in a little free speech the night before in a public lecture at which unexpectedly some Turks were present, and this morning one has been notified to go to the government palace and hand the manuscript of his talk to the pasha. To the Armenians, a phantom inscription on the palace door said: "Enter. No exit." In the Turkish Empire, only the Turks took their time; others hustled.

While the issue of freedom of speech—one of the four freedoms of the Atlantic Charter—was the immediate occasion for my taking the first formal step toward becoming an American, the other three freedoms also were inseparably involved in it. Freedom of worship, freedom from periodic wholesale looting, and freedom from periodic massacres. I need not repeat here once more the story of the 600 years of oppression of the Armenians in Turkey, a history which has ended largely because of the extinction of the Armenians as a community in the Turkish Empire.

This article is the text of an address delivered before the Daughters of the American Revolution of Pittsfield, Mass., and broadcasted through radio station WBRK on an "I Am an American" program on May 17, 1942.

Aram Boyajian is an electrical engineer, General Electric Company, Pittsfield, Mass.

As if to impress on me with the greatest possible contrast, the differences between life in the Turkish Empire and life in America, destiny guided my steps to Swarthmore, a small peaceful Quaker college town in the suburbs of Philadelphia, Pa. There I worked at tailoring and studied, studied human nature along with electrical engineering. What a contrast there was between the Turkish conception of life and the Quaker! As I was learning what is approved engineering practice, I was learning also what is approved American practice. Every year, even after I was naturalized, I was learning still more reasons, besides the four freedoms, why I would want to be nothing but an American.

During my first week in Philadelphia, the public parks and parkways, museums, various other city improvements, and public services astounded me. The mayor's slogan was "The City Beautiful." I asked an old timer how heavy were the taxes. "No personal tax," he said, "only real estate is taxed; so you pay tax indirectly in your rent." And rents were really reasonable. The Federal Government did not even tax real estate; it paid its expenses mostly from duty on imports. Honest government explained it all. Of course, I heard about a "corrupt" Vare machine in Philadelphia, but when my class visited a pumping station built by him, we found it a fine job, and Vare was proud enough of it to engrave his name prominently over the entrance. Evidently, contract corruption in the United States did not mean what it would have meant in the old country.

At a men's class, my first or second Sunday in America, a former secretary of the Commonwealth of Pennsylvania spoke eloquently on how Americans are being robbed by intrenched privilege. "Every time you buy a pound of sugar," he said, "the hand of privilege slips into your pocket and robs you." He repeated this form many times, changing each time the name of the article. I asked the man next to me how much a pound of sugar costs. "Five cents," he said. I lost all interest in the talk; the speaker was painting America black evidently as propaganda for lower tariff.

I was surprised at the appreciativeness of the average American. If you did a good job for him, he admired it loudly. Strange! In the old country, no matter how fine a job it was, we said to the cobbler, "Do you think those heels will stay on? The soles look like cardboard to me." The theory was that if you admitted it to be a fine job, he would charge more, as there was no standard price on anything. This situation also led to every business transaction's becoming a haggling contest. People forgot that its original purpose was to save money and indulged in it to prove their superior wits.

This may have sharpened their wits somewhat, but it also developed argumentativeness and faultfinding in everybody, and made human relations more difficult. In America people had discovered a better way of spending their time than haggling or arguing or faultfinding. I saw in the American experience that the art of appreciation, properly practiced, made not only for better relations all around, but also did actually save money. The workman, elated, was actually more amenable to a lower level of compensation. In politics there was of course faultfinding of a kind people called mud-slinging; but while here the worst thing that a respectable politician would call his opponent would be "an agent of big business," over there he might call him a robber of church silver.

I discovered that generosity was another characteristic of standard American practice. The grocer made sure that the scales tipped clearly in your favor; the satisfied customer would tell him sometimes not to bother about the change; the tailor did more repairs on your clothes than was included in the agreement; employers gave raises to their men without any demand from them; the department store took back the fur coat without asking if it had been worn to a wedding; and all drives for charitable purposes went over the top. It was a fantastic generous world and it ran so smoothly.

I discovered that good sportsmanship was another characteristic of good American practice. No particular word for it existed in the Near-East languages.

That the American is law-abiding was another revelation to me. While in the old country the law was made by an overlord for the purpose of hamstringing you, and you had no liking for it; here it was made by your own representatives to help make your pursuit of happiness run more smoothly, and you naturally conformed to it.

The co-operativeness of the American was a particular surprise to me. Competitors in America co-operated with each other better than partners did in the old country; and I was amazed that laws were passed here against too much co-operation. I believe America is the only country where businessmen are prosecuted for too friendly competition. Recent un-American labor troubles were of European origin and conception. I count on America's genius at co-operation to work out complete labor-capital co-operation in a few years.

Finally, I learned more about the missionary spirit in the American make-up and destiny. All of the Americans I had seen in the old country were missionaries. I had studied in their schools. So my earliest notion of an American was that of a missionary. Years have confirmed and put deeper meaning into this notion.

When I was naturalized a quarter of a century ago, I wrote home with youthful enthusiasm: "Now, Mother, I am an American, full-fledged and complete—as good as the missionaries, even as Doctor Dodd."

The answer came back in due time, "My son, your father and I are very happy that you are no more a Turkish subject, but a free American citizen. But to be a complete American must take time. You can't be as complete an American as the missionaries, as Doctor Dodd, until you are able to render to the world the kind of service that they are rendering. Don't worry about taking us there; you put your effort toward learning a profession and take your place among other Americans, and we will be happy no matter where we are."

You see, the only Americans she ever knew were missionaries; and to her, as to everybody else in her section of the world, Doctor Dodd was the angel of the Lord dispensing far and wide the blessings of American medicine and surgery. She had the notion that all Americans are missionaries, in one sense or another, sent by God to this unfortunate world.

That was the last I heard from her, and the time was the first World War. With my father and millions of other Armenians she was deported into the desert of Der El Zor as a menace to the safety of the Ottoman State; and her group, I learned later on, was crowded into a large enclosure, sprinkled with oil, and set on fire.

Well, she had gone and left me with a big order—to try to become an American like her model, Doctor Dodd of blessed memory. I spent many uneventful years after that, doing the unromantic daily tasks of my profession with the General Electric Company, until a few years ago a little incident happened that eased my mental burden a bit. My wife and I were looking for the cottage of a friend at Onota Lake, Mass., and seeing a lady, we approached her for information. She was elderly, with all-white hair, gracious in manner, and distinguished in appearance.

She gave us the information, and then she said, "You are Mr. Boyajian, aren't you?" I said, "Yes," a bit surprised. "I know you," she said, "from the first reception the Daughters of the American Revolution gave to new citizens. My name is Mrs. Irving Gamwell, regent of the DAR. I have watched you ever since, and you have been a good American."

A second World War has broken upon us now, and the meaning of being an American is being discussed widely and clarified more and more. It is penetrating now even into the heads of some of the isolationists that what the Bible says about individuals is true also about nations. A nation cannot live for itself alone or die for itself alone; the nation which tries to save itself alone is likely to perish, but one that is willing to risk itself for the larger good will survive. America is realizing its mission as a participant in the present struggle.

Were General MacArthur's Biblical allusions to our soldiers at Bataan overgenerous? Ask the Australians, the Dutch, the Filipinos, the Chinese. They'll say, "A thousand times, no." To those people, as to all other oppressed or threatened peoples of the world, an American now means a fighting missionary for the four freedoms of the Atlantic Charter.

INSTITUTE ACTIVITIES

Aid to War Effort to Be Theme of National Technical Meeting

Maximum aid to the war effort will be the principal theme for most sessions and conferences to be held during the AIEE national technical meeting in New York, N. Y., January 25–29, 1943, with headquarters in the Engineering Societies' Building. The Institute of Radio Engineers has been invited to hold their meeting concurrently. Thursday, January 28, will be devoted primarily to IRE sessions, which will be open to AIEE members, in both morning and afternoon. IRE members also are cordially invited to attend any of the AIEE sessions. The day's activities will culminate in a joint AIEE–IRE evening meeting.

Arrangements are being made for a general-session program which will present the important subject of technical manpower requirements as an aid to the war effort. Prominent speakers will present both the military and industrial phases of this subject.

MEDAL PRESENTATIONS

It is expected that the Edison Medal will be awarded in December and it will be presented Wednesday evening, January 27, 1943, together with the John Fritz and Hoover Medals in the engineering auditorium.

The John Fritz Medal was awarded to Doctor Willis R. Whitney (A '01) (*EE, November '42, p. 574, 582*) "for distinguished research, both as an individual investigator and as an outstanding and inspiring administrator of pioneering enterprise, co-ordinating pure science with the service of society through industry."

The Hoover Medal was awarded to Gerard Swope (F '22) (*EE, November '42, p. 874–5, 582*) with the following citation: "engineer and distinguished leader of industry, ever deeply interested in the welfare of his fellowmen, whose constructive public service in the field of social, civic, and humanitarian effort has earned for him the Hoover Medal for 1942."

TECHNICAL SESSIONS AND CONFERENCES

Most of the sessions and conferences of aid to the war effort will deal with getting the most out of existing equipment in order to conserve copper and other critical materials. Thus the session on capacitors will point out the savings in generator capacity and critical materials that may be effected by a more intelligent use and application of capacitors on power-transmission systems. Another session on transformer overloading will deal with the thermal protection, cooling by forced circulation, the effect of ambient temperatures, and transformer overloads for cases not covered by general rules, all of which tend toward getting the most out of existing transformers without destruction of equipment.

Among the conferences which will aid the war effort are the following: cables and overload capacities to conserve copper, changes in the design and operation of substations, power-generating equipment, up-to-the-minute discussions of defense lighting activities and developments, and guides for the selection of general-purpose motors. Since the amount of copper used to serve motors, in switches, control equipment, and distribution systems is proportional to the nameplate ratings and is from two and one-half to seven times the weight of copper in the motors a very large saving in critical materials may be made by a careful selection of motors of proper size.

As already announced (*EE, October '42, p. 527*), other "guides" or "general principles" for the selection and operation of electric equipment are in preparation in co-operation with the AIEE standards committee. Some of these guides will serve as a basis for discussions in the sessions and conferences.

The personnel of the committee making arrangements for the meeting is as follows:

Chairman, C. R. Jones (M '30); F. A. Cowan (M '29); W. S. Hill (M '30); M. D. Hooven (M '30); A. E. Knowlton (F '30); C. S. Purnell (M '35); R. L. Webb (M '35); C. C. Whipple (M '26).

Future AIEE Meetings

National Technical Meeting
New York, N. Y., January 25–29, 1943

District Technical Meeting
Pittsfield, Mass., April 8–9, 1943

District Technical Meeting
Kansas City, Mo., April 28–30, 1943

National Technical Meeting
Cleveland, Ohio, June 21–25, 1943

Board of Directors Meets

The regular meeting of the board of directors of the American Institute of Electrical Engineers was held at Institute headquarters, New York, N. Y., October 23, 1942.

The election by the executive committee, under authority of the board of directors, of C. A. Powel (F '41) as vice-president of the Institute, representing the Middle Eastern District, for the unexpired term, ending July 31, 1943, of N. S. Hibshman (F '41), who resigned because of his removal from the District, was reported. The board confirmed this action (*EE, October '42, p. 532; November '42, p. 574*).

Upon recommendation of the Providence Section and the executive committee of the North Eastern District, the board authorized changes in location and dates of the 1943 technical meeting of that District to Pittsfield, Mass., April 8 and 9. It was originally scheduled to be held in Providence, April 7–9.

Upon request of the technical program committee, the chairman of the standards committee was made an ex-officio member of the technical program committee.

Upon recommendation of the technical program committee and the technical committees concerned, the board authorized the change in status of the joint subcommittee on electronics to that of an AIEE technical committee on electronics, with the following definition of scope:

"Treatment of all matters in which the dominant factor is electronics, particularly those dealing with the design, characteristics, or behavior of devices of an electronic nature, or of the circuits associated with them. The treatment of complete equipments or systems may be considered within the scope of this committee if they depend predominantly on electronic devices for their operation. If the field of application of such equipments or systems falls also within the scope of other technical committees, action with those committees may be joint."

Authorization was given to the technical program committee to arrange with the Institute of Radio Engineers for joint sessions at the national technical meeting of the AIEE to be held in New York, N. Y., January 25–29, 1943.

Upon recommendation of the standards committee, the board approved, for submission to the American Standards Association a revision of the American Standard on "Lightning Arresters for A-C Power Circuits" (AIEE, 28; ASA, *C62*), developed by a subcommittee of the AIEE committee on protective devices, and approved the appointment of AIEE representatives on other standardizing bodies, as follows:

Walter Mikelson (A '40) to the Sectional Committee on Classification and Designation of Surface Qualities, *B46*.

H. W. Bousman (M '36) to Sectional Committee on Specifications and Methods of Test for Electrical Insulation Materials, *C59* (to replace J. A. Scott (M '34) who has entered the United States Army.)

C. A. Powel (F '41) to replace D. F. Miner (F '40) (who has entered the United States Army) on Sectional Committee *C67*, "Voltages Below 100."

Edward Bennett (F '18) as a representative of the Institute on Sectional Committee *Z10*, "Letter Symbols and Abbreviations for Science and Engineering" (transferring him from the status of Member-at-Large).

J. T. Lusignan, Jr. (M '34) as chairman of Sectional Committee *C68*, "Sphere Gap Standardization," to succeed E. J. Rutan (M '29), resigned.

C. H. Willis (F '42) as chairman of the Sectional Committee on Mercury-Arc Rectifiers, *C34*, replacing E. L. Moreland (F '21), resigned.

The Institute bylaws were amended as follows, to conform with the action of the board of directors on August 7, 1942, abolishing the Institute policy committee:

Section 65: The name of the Institute policy committee deleted from the listing under the heading "General Committees."

Section 73: The entire section deleted.

In accordance with section 22 of the bylaws, five members of the board of directors were selected to serve on the national nominating committee, namely, M. S. Coover (F '42), M. Eldredge (F '33), C. R. Jones (M '30), T. G. LeClair (F '40), E. T. Mahood (F '36).

Representatives of the Institute were appointed as follows:

Alfred Noble prize committee—Robin Beach (F '35), for the five-year term beginning January 1, 1943.

Standards Council, American Standards Association—J. R. North (F '41), for the three-year term beginning January 1, 1943; H. E. Farrer (A '21), H. L. Huber (M '23), and E. B. Paxton (M '25) as alternates for the calendar year 1943.

The personnel of the committee on award of Institute prizes, which awards the national prizes, was increased by the addition of the chairmen of the committees on communication, electrical machinery, power transmission and distribution, and protective devices.

Other actions taken by the board were as follows:

Minutes of the meeting of the board of directors held August 7, 1942, were approved.

The following actions of the executive committee on applications for election, transfer, and Student enrollment, as of September 29, 1942, were reported and confirmed: 7 applicants transferred to the grade of Fellow; 25 applicants transferred to the grade of Member; 21 applicants elected to the grade of Member; 130 applicants elected to the grade of Associate; 229 Students enrolled.

Recommendations adopted by the board of examiners at meetings held September 24 and October 15, 1942, were presented and approved. Upon recommendation of the board of examiners, the following actions were taken: 10 applicants were elected to the grade of Member; 68 were elected to the grade of Associate, and one Associate was reinstated; 311 Students were enrolled.

Monthly disbursements, reported by the finance committee, were approved as follows: August, $43,673.67; September, $19,285.97; October, $35,553.27.

A budget for the appropriation year beginning October 1, 1942, was adopted as recommended by the finance committee.

Those present at the meeting were:

President—H. S. Osborne, New York, N. Y.

Past president—R. W. Sorensen, Pasadena, Calif.

Vice-presidents—A. G. Dewars, Minneapolis, Minn.; J. Elmer Housley, Alcoa, Tenn.; C. R. Jones, New York, N. Y.; E. T. Mahood, Kansas City, Mo.; K. B. McEachron, Pittsfield, Mass.; C. A. Powel, East Pittsburgh, Pa.; E. W. Schilling, Bozeman, Mont.; Walter C. Smith, San Francisco, Calif.

Directors—M. S. Coover, Ames, Iowa; M. Eldredge, Washington, D. C.; C. M. Laffoon, East Pittsburgh, Pa.; T. G. LeClair, Chicago, Ill.; W. R. Smith, Newark, N. J.; R. G. Warner, New Haven, Conn.

National treasurer—W. I. Slichter, Schenectady, N. Y.

National secretary—H. H. Henline, New York, N. Y.

By invitation, Chairman John C. Parker of the committee on co-operation with war agencies was present during a part of the meeting.

The 1942–43 Institute Budget

In the accompanying tabulation will be found a report of the income and expenditures of the Institute for the appropriation year ending September 30, 1942, and the budget as recommended by the finance committee for the ensuing year. Approval of this budget by the board of directors, at its meeting held on October 23, 1942, was given with the understanding that practically all activities would now conform to the definition of Institute wartime policies (*EE, September '42, p. 477*). For example,

Institute Income and Expenses for the Year Ending September 30, 1942, and Budget for Year Ending September 30, 1943

	Actual Income and Expenses, Year Ending 9-30-42	Budget for Year Ending 9-30-43
Income		
Dues	$228,038.16	$210,000.00
Students' fees	14,239.25	13,000.00
Entrance and transfer fees	8,830.97	8,000.00
Advertising	71,000.83	70,000.00
Elec. Engg. — nonmember subscriptions	9,716.67	9,000.00
Trans. subscriptions	6,119.10	6,000.00
Miscellaneous sales	34,714.80	30,000.00
Interest on securities	7,696.09	6,000.00
Total	$380,355.87	$352,000.00
Expenses		
Publications:		
Text matter (Elec. Engg. & Trans.)	80,946.80*	71,065.00†
Trans. Supplement	1,525.31	750.00
Preprints	7,357.48	8,300.00
Advertising section—Elec. Engg.	27,616.91	28,200.00
Year Book	7,367.29	7,300.00
Miscellaneous expense	1,301.05	1,500.00
Institute meetings	14,111.74	14,800.00
Institute Sections:		
Appropriations	26,831.06	28,000.00
Other expenses	6,009.21	6,450.00
Institute Branches:		
Meetings expenses	922.84	1,100.00
Other expenses	2,521.65	2,950.00
Committees:		
Code of prin. prof. conduct		50.00
Edison Medal	188.10	200.00
Finance	771.23	750.00
Headquarters	27.75	100.00
Lamme Medal	20.00	275.00
Membership	9,068.32	9,200.00
Pension Fund	250.00	500.00
Research		100.00
Standards	8,517.97	11,050.00
Standards — Elec. Definitions	18,691.16	1,000.00
Technical committees	455.10	500.00
Co-operation with war agencies		500.00
Traveling Expenses:		
Geo. Dist. exec. committees	3,609.23	4,250.00
Section delegates to summer conv.	4,665.41	5,800.00
Counselor delegates to summer conv.	510.96	800.00
Dist. secys. to summer conv.	83.98	525.00
District Student conferences	6,313.80	7,950.00
President's appropriation	1,576.76	1,500.00
Vice-presidents	787.95	1,060.00
Board of directors	6,653.80	7,400.00
National nominating committee	1,121.49	1,200.00
Past presidents and other AIEE representatives		515.00
Administration:		
Headquarters' salaries	37,466.00	39,950.00
Postage	4,384.36	4,500.00
Stationery & printing	4,069.96	4,250.00
Office equipment	513.18	500.00
Trav. expense, bank charges, insurance, misc. supplies and services	4,398.07	4,750.00
Paper prizes	764.00	1,200.00
Joint activities:		
Amer. Co-ord. Com. Corrosion	25.00	25.00
American Standards Association	1,500.00	1,500.00
ECPD	1,700.00	1,700.00
Engineers Defense Board	276.67	
Engg. Foundation research projects:		
Impregnated paper insulation	250.00	
Insulating oils and cable saturants	250.00	250.00
Welding	250.00	250.00
Engg. Soc. Personnel Service, Inc.	1,152.00	1,338.00
Engg. Soc. Library	10,261.44	10,500.00
Hoover Medal		175.00
John Fritz Medal	50.00	50.00
N.F.P.A. Dues	60.00	60.00
Production clinics	130.00	
Radio broadcasts	350.00	
United Engg. Trustees building assessment	10,984.80	10,985.00
U. S. Natl. Com. I.C.I.	100.00	100.00
Miscellaneous printing, etc.		8,000.00
Authors' reprints	4,199.38	
Reprints of standards	1,949.00	
Miscellaneous	1,655.97	
Other expenses:		
Membership badges	2,325.17	2,500.00
Legal services	250.00	250.00
Pension Fund Reserve	8,000.00	10,000.00
Prepaid Elec. Engg. expenses (text paper and env. in storage)	8,997.75	9,000.00
Depreciation Reserve Fund	109.55	
Unappropriated:		
Reserved for contingencies and additional items		14,527.00
Total	$346,246.65	$352,000.00

* Actual cost $85,637.00 including expenses paid preceding year.

† Actual budget of $80,495.00 including prepaid expenses.

it is expected that Institute publications will contain, so far as possible, material which is closely related to the war effort, and that a similar policy will be followed in the selection of technical papers for presentation at national and District technical meetings, the arrangements for which will omit the usual social activities.

It will be noted that the appropriations granted for the principal activities—publications, meetings, Sections, Branches, committees, and agencies supported by the Institute jointly with other engineering societies—closely correspond with the expenditures for last year; some items are increased slightly to cover advances in prices effected since October 1941. Increases in salaries paid to headquarters' staff, affecting fifteen persons, account for approximately $3,000 of the new budget; $1,500 additional is provided in the appropriation of the administrative department for one addition to the staff.

Other budget items deserving comment are: an increase in the rate of traveling expenses for delegates to Institute meetings and to the District conferences for which such allowances are granted (from eight and one half to nine cents per mile, one way); the restoration of all national and District paper prizes; and increase of $2,000 in the annual payment to the pension fund reserve established two years ago. The budget includes $14,500 as unappropriated, for contingencies and additional expenses which may develop during the year.

Income for the coming year has been estimated below that realized last year, to allow principally for losses in dues revenue from members in military service and from overseas members. On the whole, the budget reflects the desire of the board of directors to place proper relative emphasis on the different phases of Institute activities, as modified by present-day requirements, and to limit annual expenditures to the amount of anticipated income. For the preceding year the board voted to transfer $20,000 of excess receipts over expenditures from the general funds to the reserve capital fund of the Institute.

Members wishing further particulars regarding the details underlying the various activities provided for in the budget are referred to the annual report of the board of directors for the fiscal year which ended April 30, 1942 (*EE, July '42, pp. 368-81*).

All AIEE National and District Cash Prizes Restored

At its meeting on October 23, 1942 (*this issue, p. 616*) the AIEE board of directors made provisions for the restoration of all National and District cash prizes to be awarded for the current year. Cash prizes previously were restored for all national prizes and two of the four District prizes.

All technical papers presented before the Institute are eligible under the AIEE paper prize regulations for competitive consideration for one or more of the established prizes, regardless of whether presented before a Branch meeting, a Section meeting, a District meeting, or a national convention, the several classes of prizes providing for equitable competition.

National prizes awarded are:

1. Prize for best paper in each of the following classes ($100 and certificate):
   Engineering practice
   Theory and research
   Public relations and education
2. Prize for initial paper ($100 and certificate)
3. Prize for Branch paper ($100 and certificate)

District prizes that may be awarded annually in each of the geographical Districts of the Institute are:

1. Prize for best paper ($25 and certificate)
2. Prize for initial paper ($25 and certificate)
3. Prize for Branch paper ($25 and certificate)
4. Prize for graduate student paper ($25 and certificate)

PERIODS OF COMPETITIONS

The national best paper prizes and the national initial paper prize are awarded for papers presented during the calendar year, except best paper prize in the class of public relations and education. In this class the award is for a paper presented subsequent to those considered at the time of the last previous award in this field and prior to the end of the calendar year. For the national prize for Branch paper only papers presented during the preceding academic (college) year, July 1 to June 30, inclusive, will be considered.

The District prize for best paper and the District prize for initial paper are awarded only for papers presented subsequent to the period covered by the last previous award in the class and prior to the end of the last calendar year. The District prize for Branch paper and the District prize for graduate student paper are awarded only for papers presented subsequent to the period covered by the last previous award in the class and prior to the end of the last academic (college) year, July 1 to June 30, inclusive.

TIME OF AWARDS

All of the national prizes and the District prize for best paper as well as the District prize for initial paper shall be awarded prior to May 1 of the succeeding year. The District prize for Branch paper and the District prize for graduate student paper will be awarded as determined by the District executive committee and announced to all Branches in the District.

HOW TO SUBMIT PAPERS

For the national prizes all papers approved by the technical program committee which were presented at the national conventions or District meetings will be considered for the best paper prizes and the initial paper prize without being formally offered for competition. All other papers which were presented at Section or Branch meetings must be submitted in triplicate with written communications to the national secretary on or before February 15 of the following year, stating when and where the papers were presented.

For the District prize for best paper and the District prize for initial paper papers shall be submitted at least in duplicate by authors or by officers of the Section, Branch, or District concerned to the District secretary on or before February 1. The District secretary will refer the papers either to the District executive committee or a committee appointed by the District executive committee authorized to make the awards. For the District prize for Branch paper and prize for graduate student paper papers shall be submitted at least in duplicate by authors or by officers of the Section, Branch, or District concerned to the District secretary in accordance with dates of closure and of award, as fixed by the District executive committee and announced to all Branches in the respective Districts.

The basis of evaluating all student papers has been changed to bring it more in conformity with the objectives of such papers. Those wishing further information may obtain a booklet entitled "National and District Prizes" from AIEE headquarters, 33 West 39th Street, New York, N. Y.

Bibliography to Be Published on Automatic Stations

The fourth bibliography of technical literature entitled "Bibliography on Automatic Stations, 1930–1941" is soon to be issued by the Institute. This publication sponsored by the AIEE committee on automatic stations supplements earlier bibliographies on the subject published previously in AIEE *Transactions*.

The entries in this bibliography are numbered consecutively by sections and listed alphabetically by years. The material is divided into the following sections:

General; supervisory and remote control; telemeter and telemetry; automatic and remote-controlled switches and switchgear; automatic features of generating stations using fuels; automatic boiler and combustion control; automatic hydroelectric plants; automatic substations.

The "Bibliography on Automatic Stations, 1930–1941" will be obtainable from AIEE headquarters, 33 West 39th Street, New York, N. Y., at 25 cents per copy for AIEE members (50 cents to nonmembers) with a discount of 20 per cent for quantities of 10 or more mailed at one time to one address. Remittances, payable in New York exchange, should accompany orders.

DISTRICT

District 2 Branch Counselors, Chairmen, and Officers Meet

The Student Branch meeting of the AIEE Middle Eastern District (2) held on October 16, 1942, at the University of Pittsburgh (Pa.) was attended by 73 students and 14 professors.

At the conference of counselors held during the meeting the following were appointed to serve on the district nominating committee:

L. R. Culver, University of Cincinnati (Ohio); L. J. Hodgins, University of Maryland, College Park; F. W. Smith, Lafayette College, Easton, Pa.

It was decided that the next meeting would be held at Pennsylvania State College. The exact date of the conference is to be announced.

A discussion of the effects of the accelerated wartime programs on Branch activities revealed that all schools are not following the same plan. It was urged that a greater effort be made to have juniors and sophomores take a more active part in Branch activities.

At the meeting of District 2 student Branch officers held on October 16, 1942, student chairmen of the various AIEE Branches in the District reported on the Branch activities of the past year. It was decided at this meeting that the "Shunt," official student publication of District 2 be published in six issues during the coming year. Branches were requested to support the publication editorially and financially.

A joint meeting of Student Branch chairmen and counselors was also held.

SECTION • • • • •

Washington Section Opens Season With Talk on Rubber

The AIEE Washington Section held its opening meeting of the 1942–43 season on October 13, 1942. Following dinner at the Harrington Hotel, R. A. Schatzel (A'28) director of research, General Cable Corporation, Rome, N. Y., and chairman of the technical advisory committee to the rubber and rubber products branch of the War Production Board, spoke on "Some Engineering Aspects of Synthetic Rubber." Discussing the types, properties, and availability of synthetic rubber, its properties as insulation, and its uses from an engineering point of view, he pointed out that synthetic rubbers should be considered as different types of rubber, not as separate materials different from natural rubbers. He stated further that each kind of synthetic rubber has properties that adapt it for particular uses. After the meeting refreshments were served.

ABSTRACTS • • •

Basic Sciences

43-11—Formulas for the Calculation of the Inductance of Linear Conductors of Structural Shape; *Thomas J. Higgins (A'40). 15 cents.* The formula for the geometric mean distance between two unlike rectangles with corresponding sides parallel but otherwise arbitrarily located is derived by evaluating the appropriate quadruple integral. Conjoining this formula (which encompasses, as special cases, all hitherto known formulas for the geometric mean distance between parallel-sided rectangles) with the cardinal theorem of geometric mean distance theory yields very general formulas that afford the inductance of single or of polyphase circuits constructed of linear conductors with right cross sections composed of arbitrary configurations of parallel-sided rectangles. As illustrations, specific formulas are obtained for:

1. The inductance of single-phase circuits comprised of two symmetrically located conductors, these latter being

(*a*). Channels back to back.

(*b*). Split rectangular tubular conductors.

(*c*). Two I-beams.

2. The reactance drop in an individual conductor of a three-phase circuit comprised of three equilaterally spaced, rectangular tubular conductors.

A short but comprehensive résumé of the existent literature on formulas for the calculation of the inductance of linear conductors of solid or tubular rectangular cross section is included in the paper.

Electrical Machinery

43-2—A Useful Equivalent Circuit for a Five-Winding Transformer; *L. C. Aicher, Jr. (A'37). 15 cents.* The possibilities of multiwinding transformers with five windings have been discussed by power-system engineers. Heretofore, serious consideration of such an equipment has been curtailed because of an inadequate five-winding equivalent circuit. This paper presents a practical equivalent circuit together with a development of the necessary formulas. All formulas make use of quantities that can be easily measured at the transformer terminals. Numerous mathematical manipulations in the development have been omitted from the paper in the interest of brevity. A fundamental objective has been to keep the equivalent circuit suitable for the calculating board. Examples show how the various parameters are evaluated. One example demonstrates the factors affecting the voltage regulation of a five-winding transformer when connected to two sources of energy.

43-3—A New Type of Adjustable-Speed Drive for A-C Systems; *A. G. Conrad (M'40), S. T. Smith (Enrolled Student), P. F. Ordung (Enrolled Student). 15 cents.* This paper describes the theory and characteristics of a new type of polyphase adjustable-speed motor and its control circuit. The system provides an a-c drive that has characteristics comparable to those of the Ward Leonard system used on direct currents. The control circuit provides starting as well as speed adjustments from zero to one hundred per cent of rated value. The power factor of the motor is either leading or very close to unity over its operating range, and consequently the efficiency is high. Low costs of this type of motor and its control system, the large speed range, the facility for rapid reversal by regeneration, and its high starting torque make it suitable for some industrial applications.

43-4—Performance Calculations on Tapped-Winding Capacitor Motors; *P. H. Trickey (M'36). 15 cents.* The tapped-winding capacitor motor is a capacitor motor with an extra control winding which is put in series with the motor to obtain a lower operating speed. The high-speed performance may be calculated by one of a number of recognized methods. The low-speed performance may be determined approximately by calculating the performance at a low fictitious main-winding voltage and adding to this voltage the proper impedance drops of the control winding. By trial and error this total voltage is made to equal the line voltage.

43-6—Analysis of Rating Methods for Intermittent Loads; *R. E. Hellmund (F'13) (deceased). 15 cents.* The economies of rating structures for electrical equipment from the viewpoints of manufacturing and application are considered. Methods for dealing with periodic load applications are reviewed. The present methods have certain limitations for some practical applications. An improved method for applying motors to load cycles where the motor stops and ventilation changes in the no-load part of the cycle is developed. This method is based on certain motor-loss ratios, that is, the relation between the losses dissipated with standard temperature-rise running and at standstill and the losses at the nominal or short-time rating. It permits the application of service-factor or short-time ratings to periodic loads with more exactness. Attention is called to the wide variations in permissible rms current on periodic loads for different loss ratios and changes in ventilation on a motor.

Instruments and Measurements

43-1—An Instrument for the Determination of Contact-Making and Breaking Time; *Walther Richter (F'42), W. H. Elliot (A'40). 15 cents.* Hitherto for the measurement of the arcing time of contacts, opening or closing a circuit, oscillograms have had to be taken. This procedure is tedious and time consuming, especially when many observations are required. An electronic device has been developed which indicates the time of arcing on a direct-reading instrument. Four ranges, 2, 5, 10, and 20 milliseconds, for full scale deflection are available. Use is made of the well-known method of charging a condenser with a definite current, the charge accumulated being a measure of the duration of current flow. The novel feature is the method of causing the charging current to flow only during the arcing time of the contacts under investigation. A Wheatstone bridge with a vacuum tube in one arm is used to indicate the condenser charge on the direct-reading instrument.

Power Transmission and Distribution

43-7—Abnormal Overvoltages Caused by Transformer Magnetizing Currents in Long Transmission Lines; *H. A. Peterson (M '41), T. W. Schroeder (A '37). 15 cents.* It has been found by means of the transient analyzer that certain combinations of transmission-line length and system reactance can give rise to overvoltages of considerable magnitude when a lightly loaded transformer is connected to that transmission line. Such a system is most susceptible to overvoltage phenomena when the transformer is connected to the receiving end of that transmission line, although, theoretically, similar phenomena may occur for any transformer location including the transformer at the sending end. These overvoltages are in addition to the normal fundamental-frequency voltage rise and are of a combination subharmonic and harmonic nature. A comprehensive analysis of representative circuits with and without series-capacitor compensation has been made to determine the regions of abnormal behavior and the effectiveness of various practical methods for safely limiting such overvoltages. Such practical means for controlling the overvoltage include proper switching and relaying arrangements, the synchronous condenser, shunt reactors, and resonant shunts. These results will be of considerable importance from the standpoint of designing and operating systems involving the transmission of power over distances in the practical range above 200 miles.

43-9—Equivalent Circuits for Oscillating Systems and the Riemann–Christoffel Curvature Tensor; *Gabriel Kron (A '30). 15 cents.* It is shown that a necessary though not sufficient condition for the existence of a physical model, corresponding to a given set of equations, is that the set should be a tensor equation. This follows from the fact that only quantities that are tensors can be measured by instruments. The conclusion is therefore reached that only equations that are in tensor form can be set up on the a-c calculating board. The principle is illustrated by setting up equivalent circuits with the aid of tensorial hunting equations for the determination of the steady-state, hunting, and self-excitation performance of two interconnected instrument-selsyns, of two salient-pole synchronous machines, and of a capacitor-compensated transmission line connected to a salient-pole synchronous machine. A companion paper, 43-8, contains the results of an extended study made on the a-c network analyzer with the aid of one of the equivalent circuits developed here.

43-8—Self-Excited Oscillations of Capacitor-Compensated Long-Distance Transmission Systems; *R. B. Bodine (A '37), C. Concordia (M '37), G. Kron (A '30). 15 cents.* This paper presents the results of an investigation of regions of possible instability which may be encountered in the transmission of electric power for long distances over series capacitor-compensated lines. The limits of stability, hunting, and self-excitation applicable during normal steady-state operation are determined. The method described can be used to determine the effects of length of line, amount of compensation, machine and line characteristics, and so forth. Results showing the effects of compensation and of machine characteristics and loading are given. The system performance was determined by calculations and measurements on equivalent circuits set up on the a-c network analyzer. The method used is believed to have certain advantages, particularly in the range of system parameters which can conveniently be considered, and in the accuracy of representation of the system, over those used in previously described investigations. It is concluded that the types of instability considered are avoidable in practical applications of long-distance electric-power transmission if attention is paid to proper design. An effective amortisseur is particularly desirable.

43-10—Dielectric Recovery Characteristics of Large Air Gaps; *G. D. McCann (A '38), J. J. Clark (A '40). 15 cents.* The rates at which large air gaps recover dielectric strength after discharging short-duration surge currents comparable to those produced by lightning are of fundamental importance in relation to the mechanism of natural lightning and in determining power-system performance. For the study of this problem a method of synchronizing two surge generators has been developed so that a surge discharge can be produced and controlled surge voltage applied after specified time intervals. By this means a study of the rate of dielectric recovery and the visual mechanism of successive discharges has been made for a wide range of initial discharge currents. The data obtained are compared with the mechanism of multiple strokes and are applied to the problem of determining the probability of transmission-line lightning flashovers producing an outage and to the minimum safe reclosing times after circuit interruption.

Protective Devices

43-5—Correlation of System Overvoltages and System Grounding Impedance; *working group on correlation of system-grounding impedance of the committee on protective devices. 15 cents.* The various causes of system overvoltages other than lightning and the effect of the different classes of system grounding on these overvoltages are examined in this paper. If in a given system or portion thereof, $X_0/X_1<3$ and $R_0/X_1<1$ for all fault locations and for any condition of operation and amount of connected generating capacity, then that system or portion thereof is classified as solidly grounded. Such a system has the lowest overvoltages and protective level. If greater limitation of short-circuit current is desired, resistance or reactance may be introduced in the neutral. The permissible value of reactance lies in the range from $X_0/X_1=3$ to 10 for generator neutral grounding, although sometimes it may be higher for grounding transmission systems. The maximum permissible value of resistance for neutral grounding corresponds to the tuned reactance value of the ground-fault neutralizer. The isolated neutral system is poorest with respect to overvoltages and protective level.

STANDARDS . . .

AIEE Standards Manual Published

The standards committee of the Institute announces the publication of the "AIEE Standards Manual." The manual has been in course of development for several years, having been started by R. E. Hellmund during his chairmanship of the standards committee. His preliminary draft has been revised and developed by a subcommittee under H. R. Summerhayes (F '39). The pamphlet, it is hoped, will prove to be an effective guide to all committee chairmen and others engaged in the many phases of standards work now carried on by the AIEE. For many years it has been evident that there was a general lack of understanding of the machinery of standardization and the relationships existing particularly as to committees engaged in activities of joint interest to AIEE and the American Standards Association. The "AIEE Standards Manual" outlines in detail the various steps to be followed in standards work of such joint interest. Excerpts from the bylaws governing standards are given as well as the bylaws of the standards committee. Additional sections deal with the work of the various standards co-ordinating committees, basic guiding standards, test codes, electrical definitions, and publication procedure.

The standards committee urges that those engaged in standards work obtain a copy of this pamphlet, a careful reading of which should result in a much clearer understanding of the rather complex machinery which must be put in operation when new projects or revision of existing projects are undertaken. Copies may be obtained gratis from AIEE headquarters by writing H. E. Farrer, secretary of the standards committee.

Comments on Trial Standard Asked

AIEE Trial Standard 31, "Capacitance Potential Devices and Outdoor Coupling Capacitors," which was made available in December 1941 for trial use is to be revised and issued as a permanent standard. In its trial form it covers definitions, ratings, and accuracy performance characteristics of capacitance potential devices and outdoor coupling capacitors. Since sufficient time has elapsed to permit some use of the standard, the working group which developed it desires from interested engi-

neers comments and suggestions which may be helpful in reshaping the standard for permanent use. All communications should be addressed to C. A. Woods, Jr., AIEE relay subcommittee of the committee on protective devices, switchgear engineering department, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

PERSONAL • • • •

O. A. Knopp (A '09, M '34) chief, bureau of tests and inspection, Pacific Gas and Electric Company, Emeryville, Calif., has recently retired. He was born August 24, 1877, in Berlin, Germany, and received his technical education in that country. His electrical engineering career in America began in 1901, when he accepted an invitation to introduce German technical methods to the students of the Blue Hill Observatory, Harvard University, Cambridge, Mass. Prior to this appointment he had been associated briefly with Bergmann Electrical Works, Berlin, Germany, as testing engineer, and as engineer at the Aeronautical Observatory of the Royal Prussian Meteorological Institute at Berlin. From 1902 to 1903 he was associated with General Electric Company in the testing department, learning the American methods of electrical engineering, and upon completion of the test course joined the Oakland Gas, Light and Heat Company as assistant superintendent. He became assistant superintendent of the Sacramento (Calif.) Gas and Electric Railway Company in 1904, and in 1905 he returned to Oakland (Calif.) Gas, Light and Heat Company, where he remained as superintendent of the meter and instrument department until 1908. From 1908 to 1915 he was superintendent of the standardizing laboratory and meter department, Pacific Gas and Electric Company, Oakland, Calif., and from 1915 to 1920 he served as superintendent of the laboratory department of the company's general office in San Francisco. In 1920 he was appointed chief, bureau of tests and inspection, a position he held until his retirement. He also is a member of the American Society for Testing Materials. In 1937 he was awarded the AIEE Pacific District Prize for initial paper for his paper "Some Applications of Instrument Transformers."

C. B. Joliffe (M '34) assistant to the president of the Radio Corporation of America and chief engineer, RCA Laboratories, New York, N. Y., has been appointed vice-president and chief engineer of the RCA Manufacturing Company, New York, N. Y. He has been associated with the RCA company since 1935. Doctor Joliffe was born November 13, 1894, in Mannington, W. Va., and received the degrees of bachelor of science (1915) and master of science (1920) from West Virginia University, where he served as instructor in physics from 1917 to 1918 and from 1919 to 1920. During World War I he served in the United States Army Signal Corps. From 1920 to 1922 he was instructor of physics at Cornell University, Ithaca, N. Y., and in 1922 he was awarded the degree of doctor of philosophy from that institution. From 1922 until 1930 he was associate physicist to the physicist, radio section, Bureau of Standards, Washington, D. C. He served as technical advisor for the International Consulting Committee on Radio at The Hague in 1929. In 1930 he was made chief engineer of the Federal Radio Commission, and when this was replaced in 1934 by the Federal Communications Committee, which is charged with regulation of all wire and radio communications, he became chief engineer of the new commission. In 1935 he was appointed engineer in charge of the RCA Frequency Bureau, Radio Corporation of America, later becoming chief engineer of the RCA Laboratories. He served on the AIEE committee on communication from 1934 to 1939 (chairman, 1936–37), and on the technical program committee for 1936–37. He is a fellow of the Institute of Radio Engineers and the American Association for the Advancement of Science, and a member of Phi Beta Kappa and Sigma Xi.

M. T. Crawford (A '07, F '22) assistant chief engineer, Puget Sound Power and Light Company, Seattle, Wash., has recently been appointed chief engineer. He has been associated with the company and its predecessors since 1906. He was born March 3, 1885, in Louisville, Ky., and received the degrees of bachelor of science in electrical engineering (1907) and a master's degree in electrical engineering (1910) from the University of Washington. He joined the Seattle-Tacoma Power Company in 1906 as draftsman and record clerk, becoming general foreman in charge of construction (1909), and assistant engineer (1910). In 1912 he was appointed superintendent of the transmission division, water power department, Puget Sound Traction Light and Power Company, when the Seattle-Tacoma Power Company consolidated with several other utilities. During this year he also acted as engineer for the Key City Light and Power Company, Port Townsend, Wash. He held successively the positions of superintendent of the transmission and distribution divisions, light and power department (1916–18) and acting general superintendent of the light and power department (1917). In 1919, when the company was reorganized, he became superintendent, distribution department, Puget Sound Power and Light Company, Seattle, Wash. He was appointed to his latest position in 1935. He is a member of Tau Beta Pi and Sigma Xi.

W. M. White (M '21) manager and chief engineer, hydraulic department, Allis-Chalmers Manufacturing Company, Milwaukee, Wis., retired recently. His retirement concludes a 31-year association with the company. He was born November 20, 1871, at Valley Head, Ala., and was graduated from Tulane University with the degree of bachelor of science in electrical engineering in 1899. He began his electrical engineering career in 1899 as assistant engineer, Drainage Commission of New Orleans, La., and from 1899 to 1901 he was in charge of the steam generating station, transmission lines, and the motor-driven pumping stations. From 1901 to 1911 he was associated with I. P. Morris Company, Philadelphia, Pa., as hydraulic engineer, and since 1911 he has held his present position with the Allis-Chalmers company. In 1930 he was awarded the honorary degree of doctor of science by Tulane University, and in 1940 he was named a Modern Pioneer by the National Association of Manufacturers. Doctor White is the holder of numerous patents and the author of several technical articles and papers. He served as a member of the AIEE committee on power generation from 1924 to 1929. He is also a member of the American Society of Mechanical Engineers, the American Society of Civil Engineers, the Franklin Institute, Sigma Xi, and Tau Beta Pi.

Gordon Thompson (A '12, M '18) assistant chief engineer, Electrical Testing Laboratories, Inc., New York, N. Y., has recently been appointed chief engineer. He was born in Charlestown, N. Y., on October 18, 1888, and received the degrees of bachelor of engineering (1909) and electrical engineer (1913) from Cooper Union. He entered the employ of Electrical Testing Laboratories in 1909, and was made foreman of the electrical laboratory in 1910. In 1913 he became engineer in charge of the electrical laboratory, serving in that capacity until 1919. During the years 1911–15 he also taught physics and electrical engineering courses at Cooper Union. From 1919–26 he was professor of electrical engineering at Nanyang University, Shanghai. He also served as consulting engineer for the Mission Architects Bureau of China, and designed illumination and electrical distribution apparatus in several public buildings in Changsa and Wuhu. He returned to Electrical Testing Laboratories in 1927 as assistant chief engineer. He has served on the AIEE standards committee since 1939, and the committee on domestic and commercial applications since 1941. He is also a member of the American Society for Testing Materials.

H. F. Boe (M '30) manager, district manufacture and repair department, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., has been elected vice-president of the company. He was born May 12, 1884, at Mansfield, Ohio, and first joined the Westinghouse company in 1903 in the testing department. Until 1910 he worked on dynamo and motor testing, ultimately becoming foreman in charge of testing in the department. In 1911 he was transferred to general engineering, and in 1913 he was appointed engineer in the sales engineer department. He

left the company in 1916 for a brief association with the Vaile Kimes Company, Dayton, Ohio (1916–17), returning to the Westinghouse company's office in Rochester, N. Y., as sales engineer in 1918. In 1922 he was transferred to the Buffalo, N. Y., office as manager of the industrial division, and in 1925 he became manager of the Buffalo district. He was appointed manager of the eastern district, New York, N. Y., in 1935, commercial manager, East Pittsburgh, Pa., in 1937, and in 1938 he was appointed manager of the district manufacture and repair department. He received the Westinghouse Order of Merit in 1941.

K. R. Van Tassel (A'27) manager of sales, Lynn Motor Section, motor division, River Works, General Electric Company, Lynn, Mass., has been appointed manager of sales of the company's new integral-horsepower motor section of the motor division. He has been associated with General Electric Company since 1925, the year in which he received the degree of bachelor of science from Massachusetts Institute of Technology. After one year in the student engineering course in Schenectady, N. Y., he was transferred in 1926 to the transformer engineering department, Pittsfield, Mass. In 1928 he was transferred to the single-phase motor department, Pittsfield, ultimately becoming designing engineer for the department. In 1932 he was sent to the fractional-horsepower motor engineering department, Fort Wayne, Ind., later becoming a commercial engineer in this department, and finally, staff assistant to the manager of the Fort Wayne Works. He became manager of sales at the River Works in 1940.

H. V. Putman (A '23, M '32) manager, transformer division, Westinghouse Electric and Manufacturing Company, Sharon, Pa., has been elected a vice-president. He has been associated with the company continuously since 1925. He was born in Barker, N. Y., on May 27, 1899, and received the degrees of bachelor of science in electrical engineering (1920), master of science in electrical engineering (1921), and doctor of philosophy (1923) from Union College. Following a two-year association (1923–25) with Ideal Electric and Manufacturing Company, Mansfield, Ohio, first as design engineer and later as assistant chief engineer, he entered the employ of the Westinghouse company as design engineer on synchronous motors. He was appointed section engineer in the synchronous motor section (1928), assistant manager of the transformer engineering department (1929), manager of the transformer engineering department (1931), and was named manager of transformer division in 1941.

J. F. Wiggert (A '09, M '16) chief electrical engineer, Homestake Mining Company, Lead, S. D., retired recently. Following a five-year association with Edison Light and Power Company, La Crosse, Wis., and brief affiliations with various other companies, he entered the employ of the Homestake company in 1902 as assistant to the chief electrical engineer. He was associated with the development of electrical equipment at the Homestake Mining Company, almost from its beginning, including supervision of the development of the carrier-current system of shaft signalling. In 1923 he was appointed chief electrical engineer, a position he held until his retirement. He is also a member of the American Institute of Mining and Metallurgial Engineers, and a consulting engineer with the United States Bureau of Mines. He was born in La Crosse, Wis., on September 22, 1877.

C. L. Gust (A '15) assistant chief electrical engineer, Homestake Mining Company, Lead, S. D., has been appointed chief electrical engineer. He has been associated with the company since 1912, and has served as assistant chief electrical engineer since 1923. Prior to his appointment to that position he served in various other capacities, including operator at the Lead, S. D., substation and the Spearfish, S. D., hydroelectric power station, chief engineer at the Englewood, S. D., power station, chief operator of the Lead, S. D., substation, and foreman of electrical installation at the Lead, S. D., steam-turbo-generator station. He was born July 21, 1894, at Yankton, S. D., and received his first engineering experience from 1910 to 1912 with Stone and Webster Engineering Corporation, Everett, Wash., and Oregon Washington Water Power Company, Medford.

J. R. Read (A '04) president, Canadian Westinghouse Company, Ltd., Hamilton, Ont., has been appointed president of the Atlas Plant Extension, Ltd., Welland, Ont. He was born in Virginia, on October 24, 1879. He began his electrical engineering experience with Sterling White Lead Company, Parnassus, Pa., afterward spending three years as student engineer with Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa. He was employed as engineer by the Cherokee Gold Dredging Company, and the Northern California Power Company before entering the employ of Canadian Westinghouse in 1904 as sales engineer for the Vancouver (B. C.) district office. He was appointed Vancouver district manager in 1907, company vice-president in 1937, director in 1938, and president in 1939.

A. M. Unger (A '40) development engineer, Pullman Standard Car Manufacturing Company, Chicago, Ill., was the recipient of the $3,700 first award in the $200,000 Progress Program of the James F. Lincoln Arc Welding Foundation. His paper, written in collaboration with J. E. Chandlin, Jr., discussed the major considerations in the gradually increasing applications of welding to railroad passenger cars since 1936. **J. F. Petrowsky** (A '17, M '20) designing engineer, Burke Electric Company, Erie, Pa., was awarded $150 for his paper entered in the classification of functional machinery, and **L. J. Bentley** (A '40) design leader of maintenance department, Chevrolet Gear and Axle, Detroit, Mich., received a $100 award in the same classification.

F. M. Farmer (A '02, F '13) vice-president, and until recently, chief engineer of Electrical Testing Laboratories, Inc., New York, N. Y., has been appointed consulting engineer. He has been associated with Electrical Testing Laboratories since 1903, when he joined the staff as technical assistant. He became engineer for the company in 1906, chief engineer in 1912, and vice-president in 1929. He has been serving the United States government lately in the capacity of co-ordinator of inspection for the New York Ordnance district. He was a member of the AIEE board of directors (1934–38), and is a past vice-president (1938–39) and past president (1939–40) of the Institute.

W. D. Coolidge (A '10, M '34) vice-president and director of research, General Electric Company, Schenectady, N. Y., was awarded the honorary degree of doctor of laws by Ursinus College in recognition of his x-ray and other research work. The recipient of the AIEE Edison Medal in 1927, and the Washington Award in 1932, he received the degrees of bachelor of science in electrical engineering (1896) from Massachusetts Institute of Technology, and doctor of philosophy (1899) from the University of Leipzig.

J. W. E. Griemsmann (A '39) research laboratories, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., has recently been appointed to the staff of the graduate electrical engineering department of the Polytechnic Institute of Brooklyn, N. Y., as a research and teaching assistant. He received the degrees of bachelor of electrical engineering (1936) and master of electrical engineering (1938) from that institution.

R. C. Muir (A '08, F '36) vice-president in charge of engineering, General Electric Company, Schenectady, N. Y., has been granted the honorary degree of doctor of engineering by Manhattan College. He was graduated from the University of Wisconsin in 1905 with the degree of bachelor of science in electrical engineering, and was awarded the honorary degree of doctor of engineering from that institution in 1939.

P. S. Mancini (A '37) assistant engineer, Public Service Engineer's Office, Providence, R. I., has become public service engineer for that city. He was graduated from Massachusetts Institute of Technology in 1926 with the degree of bachelor of science, and has been associated with the public service engineering department of Providence since 1931.

W. L. Winter (A '21) sales engineer, Westinghouse Electric and Manufacturing Company, Salt Lake City, Utah, has been ap-

pointed sales engineer, central station and transportation divisions, San Francisco, Calif. He entered the employ of the Westinghouse company in 1916, and has worked in the Salt Lake City office since 1933.

A. R. Stevenson, Jr. (A '20, F '37) staff assistant to the vice-president in charge of engineering, General Electric Company, Schenectady, N. Y., has been elected to serve as a manager of the American Society of Mechanical Engineers.

L. A. Goalby (A '40) central station division, Westinghouse Electric and Manufacturing Company, Denver, Colo., has been appointed manager of the Denver office. He has served as apparatus sales engineer in this office since 1936.

G. T. Shoemaker (M '20, F '39) vice-president, Kansas City (Mo.) Power and Light Company, has been nominated vice-president of the American Society of Mechanical Engineers for 1942–43.

OBITUARY • • • •

John Harisberger (A '02, M '10) retired general superintendent and manager, division of power supply, Puget Sound Power and Light Company, Seattle, Wash., died on September 9, 1942. He was born December 2, 1871, in Wasen, Switzerland, and became a naturalized citizen of the United States in 1904. He first became identified with the electrical industry in 1892, when he was employed by the Northwest Thomson-Houston Electric Company and Northwest General Electric Company, Portland, Oreg., as shop apprentice and construction worker. From 1894 to 1895 he was employed by the Union Power Company, Portland, Oreg., as repair man and station operator, and from 1896 to 1898 he was associated with Portland (Oreg.) General Electric Company in electrical construction. In 1899 he was placed in charge of construction of the hydroelectric plant at Snoqualmie Falls, and upon its completion he became general superintendent of the Snoqualmie Falls (Wash.) Power Company. In 1903 he was appointed in addition general superintendent and electrical engineer for the Seattle (Wash.) Cataract Company and the Tacoma (Wash.) Cataract Company, retaining his position when the three companies combined to become the Seattle-Tacoma Power Company. In 1912, upon consolidation of the electric companies in the Puget Sound district, including the Seattle Electric Company, Seattle-Tacoma Power Company, Pacific Coast Power Company, and Puget Sound Power Company, he was appointed superintendent of water power in charge of all hydroelectric stations of the Puget Sound Traction, Light and Power Company, Seattle, Wash., which assumed its present name in 1919. In 1931 he was appointed general superintendent and manager of the division of power supply, in charge of all hydroelectric and steam-power-producing facilities and trunk transmission lines, a position he held until his retirement in 1938. From 1905 to 1911, Mr. Harisberger was a lecturer on the transmission and generation of electrical energy at the University of Washington, Seattle. He also served on the AIEE committee on power stations from 1916 to 1917, and from 1923 to 1924.

Frank Reith Phillips (M '27) chairman of the board of directors and the executive committee and former president of the Philadelphia (Pa.) Company and subsidiary companies, Pittsburgh, Pa., died October 23, 1942. He was born on October 29, 1876, in Cleveland, Ohio, and studied engineering by private tutor and at the Case School of Applied Science. In 1894 he joined the engineering department of the Cleveland (Ohio) City Railway Company, and in 1903, when the company was consolidated with the Cleveland (Ohio) Electric Company, he was appointed assistant master mechanic. From 1904 to 1906 he was master mechanic with the Cincinnati, Newport, and Covington Light and Traction Company, and in the latter year he was appointed chief engineer of the Michigan United Railways Company. After brief associations with the Ohio Brass Company, Mansfield, and the United Light and Traction Company, Mich., he entered the employ of the Pittsburgh (Pa.) Railways Company, a subsidiary of the Philadelphia Company, in 1910. He served successively as superintendent of equipment and acting general manager, and was ultimately appointed mechanical and electrical engineer. In 1926 he was appointed vice-president and general manager of the Duquesne (Pa.) Light Company, a Philadelphia Company subsidiary, and retaining this office, in 1928 became vice-president of the Equitable Gas Company, another subsidiary. He was named senior vice-president of the Philadelphia Company in 1929, and later in the same year was appointed president of all the subsidiaries of that company, including the Pittsburgh Motor Coach Company, the Alleghany County Steam Heating Company, and the Equitable Sales Company. In 1931 he became president and director of the Philadelphia Company as well as of the subsidiaries, a position he held until his recent resignation. He was active in the design of the first efficient double-deck motor cars, Pittsburgh, 1917, and originator of the low floor street car, involving the principle of small diameter wheels, since largely adopted by street-railway manufacturers and automobile designers.

Lester William Wallace Morrow (A '13, M '17, F '25) professor of engineering administration, Rutgers University, New Brunswick, N. J., died November 15, 1942. He was born August 7, 1888, in Hammond, W. Va., and received the degree of mechanical engineer from Cornell University in 1911. After a year as instructor in electrical engineering at Cornell University, Ithaca, N. Y., he became assistant professor of electrical engineering at the University of Oklahoma, Norman, in 1913. He became associate professor of electrical engineering in 1916, and in 1917 was made professor and director of the school of electrical engineering. During 1918–19 he was assistant director of the United States Army Signal Officer's School at Yale University, New Haven, Conn., and from 1918 to 1922 he was assistant professor of electrical engineering, Yale University, acting as senior professor and executive of the electrical engineering department. In 1922 Mr. Morrow joined the McGraw-Hill Publishing Company, Inc., New York, N. Y., as associate editor of *Electrical World*, becoming managing editor (1924) and editor (1928). In 1936 he resigned to become general manager, fiber products division, Corning (N. Y.) Glass Works, and in 1939 he returned to the McGraw-Hill Publishing Company as regional editorial director, Chicago, Ill. He is the author of a book on electric power stations, and numerous technical articles. He has served as chairman of the AIEE technical program (1923–25), and Edison Medal (1938–41) committees, and as chairman of the committees on the award of Institute prizes (1923–25), co-ordination of Institute activities (1934–37), consitution and bylaws (1937–39). He was serving currently as a member of the board of examiners and of the Edison Medal committee. He was also a member of Sigma Xi, American Society of Mechanical Engineers, American Electrochemical Society, and a grand counselor of Sigma Tau.

Joseph Franklin Porter (A '87, M '87, F '33) chairman of the board of directors, Kansas City (Mo.) Power and Light Company, died on November 7, 1942. He was born on June 27, 1863, in Harrison County, Iowa, and was graduated from Iowa State College in 1884 with the degree of bachelor of civil engineering. He began his career in electrical engineering in 1885 with the Des Moines (Iowa) Edison Light Company, and worked briefly in various capacities for the Appleton (Wis.) Edison Light Company, the Western Edison Light Company, Chicago, Ill., and the Abilene (Tex.) Water and Light Company. In 1887 he joined the Western Electric Construction Company, St. Louis, Mo., as foreman, and later in the same year established his own business as construction contractor. In 1889 he sold out to Edison United Manufacturing Company, New York, N. Y., and joined that company's New York office, remaining until 1891. From 1891 to 1892 he was associated in street railway supply and electrical equipment business in New York, N. Y., and in Pittsburgh, Pa., and in 1892, with J. G. White, he undertook a contract for the equipment of the Kansas City (Mo.) Elevated Railway Company. From 1893 to 1906 he was president of the Alton (Ill.) Railway, Gas, and Electric Company, and from 1906 to 1917 he was president of the Tri-Cities Railway and Light Company, of Davenport, Rock Island, and Moline, Ill. He served concurrently as chairman of the executive com-

mittee of United Light and Railways and president of the Cedar Rapids and Marion (Iowa) City Railway Company from 1912 to 1915, and was appointed president of the Kansas City Power and Light Company in 1917. He became chairman of the board of directors in 1939. He was a member of Tau Beta Pi and the American Society of Mechanical Engineers.

Frederick Walter Hadley (A '96, M '01) retired safety director, Georgia Power Company, Atlanta, died on June 7, 1942. He was born on July 13, 1871, in Winterset, Iowa, and was graduated from Massachusetts Institute of Technology in 1893 with the degree of electrical engineer. He immediately entered the employ of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., in general shop work and electrical testing, and in 1895 he was transferred to the design of electrical apparatus. He was associated with the West End Street Railway Power Plants, Boston, Mass., from 1896 to 1898, and in the latter year he entered the employ of Westinghouse Church Kerr and Company, Engineers, Boston, Mass., first as draftsman on plans for mechanical equipment and later as inspecting engineer on the erection and installation of electrical equipment for the South Union Station terminal, Boston, Mass. In 1899 he entered the employ of the Boston (Mass.) Terminal Company as assistant superintendent of the steam power plant, becoming superintendent in 1900. He returned to Westinghouse Church Kerr and Company, Engineers, in 1901, working in their New York office from 1901 to 1904. From 1904 to 1912 he was superintendent of the hydroelectric plant, Atlanta (Ga.) Water and Electric Company, and from 1912 to 1927 was superintendent of the hydroelectric plants, Georgia Railway and Power Company. In 1928 he was appointed to the safety and welfare department, Georgia Power Company, Atlanta, as safety director, a position he held until his retirement in 1931.

Herbert Benjamin Coho (A '94) proprietor, Coho Chemical Company, New York, N. Y., died on May 19, 1942. He was born on August 21, 1869, in Ashland, Pa., and began his engineering career in 1890 as a member of the Edison student course, Schenectady, N. Y. From 1891 to 1892 he was successively in charge of the company's Sprague tests and in service in the armature department and wiring tests. He became associated with the Wardell Engineering Company, Bridgeport, Conn., from 1893 to 1895, assisting in the installation of copper-zinc storage-battery systems. In 1895 he established the H. B. Coho Company, and until 1909 independently installed electrical plants in New York, N. Y. He also constructed a water-power development in Sullivan County, N. Y., during this period. From 1909 to 1915 he served as research engineer and assistant secretary for the United Lead Company, becoming, in the latter year, personnel director of the United States Cartridge Company, Lowell, Mass. During World War I he served in the United States Quartermaster Corps. In 1919 he became secretary of the New York Lumber Trade Association, remaining in this capacity and secretary of the New York Lumber Trade Association Club until 1932. In 1933 he founded the Coho Chemical Company, New York, N. Y., and was head of the company until his death.

Nelson Jarvis Darling (M '34) manager of the West Lynn and River Works, General Electric Company, Lynn, Mass., died October 26, 1942. He was born on August 7, 1884, in Toronto, Ont., and was graduated from Cornell University in 1907 with the degree of mechanical and electrical engineer. From 1907 to 1909 he was associated with the General Electric Company, Schenectady, N. Y., as student engineer in the test course, and in the latter year spent six months in the Panama Canal Zone on loan to the Dodge and Day Company, to assist in the installation of electrical apparatus. He returned to the General Electric Company to serve in the railroad engineering department at Schenectady from 1909 until 1915, when he was placed in charge of the mechanical and electrical engineering work at the General Electric plant at Erie, Pa. In 1918 he was made assistant works manager at Erie, Pa., and in 1922 he was appointed manager of the River Works, West Lynn, Mass. In 1935 he was named manager of the West Lynn Works in addition. During World War I he was engaged in the designing of machines for the manufacture of turbines and projectiles. He was a member of the AIEE committee on education from 1926 to 1928.

John Harcourt Williams (M '13) deputy chief electrical engineer, Manchester Corporation Electricity Department, England, died in July 1942. He was born on November 21, 1876, in Croydon, England, and received his technical education at the City and Guilds College, London, and as a student engineer from 1897 to 1899 with Messrs. Maudsley, Sons and Field, Marine Engineers, Lambeth, London. In 1900 he began a 30-year affiliation with the British Thomson-Houston Company, Ltd., Rugby, London, serving on the company's outside construction staff in Hull, Huddersfield, and Nottingham, until 1912. In that year he was appointed to the London staff, where he remained in charge of all construction work for the British Thomson-Houston Company in the London district until 1920. In 1920 he was transferred to Manchester as district manager of the North-West England district, and in 1930 he left the company to become deputy chief engineer to the Manchester Corporation Electricity Department. This position he held until his death in July, only three months before he was to have retired. He was also a member of the Institution of Electrical Engineers and the Institution of Civil Engineers, England.

Frank Eugene Danford (A '37) engineering department, General Electric Company, Schenectady, N. Y., died October 11, 1942. He was born on January 13, 1916, in Hutchinson, Kansas, and was graduated from Kansas State College in 1936 with the degree of bachelor of science in electrical engineering. He immediately entered the employ of the Kansas Power and Light Company, Hutchinson, as meter tester in the service engineering department. In 1937 he joined General Electric Company, Erie, Pa., as student engineer in the testing department. After a brief period in the Erie Works he was transferred to the engineering department of General Electric in Schenectady, N. Y., where he remained until his death. He had served for several years on the executive committee of the AIEE Schenectady Section.

MEMBERSHIP

Recommended for Transfer

The board of examiners, at its meeting on November 19, 1942, recommended the following members for transfer to the grade of membership indicated. Any objection to these transfers should be filed at once with the national secretary.

To Grade of Fellow

Battey, W. R., chief electrical engineer, Basic Magnesium, Inc., Las Vegas, Nev.
Forman, A. H., professor and head of department of electrical engineering, West Virginia University, Morgantown, W. Va.
Lindvall, F. C., professor of electrical engineering, California Institute of Technology, Pasadena, Calif.
Morrison, J. W., vice-president and chief engineer, Rochester Telephone Corporation, Rochester, N. Y.

4 to grade of Fellow

To Grade of Member

Ahlquist, R. W., teaching, Iowa State College, Ames, Iowa.
Amador, F. J., electrical engineer, The Panama Canal, Diablo Heights, Canal Zone.
Anderson, H. L., engineer, Commonwealth and Southern Corporation, Jackson, Mich.
Archer, L. B., assistant professor of electrical engineering, University of Illinois, Urbana, Ill.
Blakely, R. T. C., assistant engineer, International Business Machines Corporation, New York.
Burns, L. L., engineer, Southwestern Bell Telephone Company, Dallas, Texas.
Campbell, G. H., sales engineer, General Electric Company, New York.
Hegensick, district sales and service engineer, The Pyle National Company, St. Paul, Minn.
Hanstein, H. B., teaching electrical engineering, College of the City of New York, New York.
Harrap, G. V., technical assistant, West Ham Corporation, London, England.
Hiegel, A. J., design engineer, General Electric Company, Fort Wayne, Ind.
Holladay, C. H., treasurer, Los Angeles Railway Corporation, Los Angeles, Calif.
Mathis, J. D., Southwestern Bell Telephone Company, Dallas, Texas.
Milligan, L. S., assistant electrical engineer, George G. Sharp, New York.
Parker, W. A., lieutenant, United States Navy, Galveston, Texas.
Quistorff, W. W., assistant superintendent of distribution, Puget Sound Power and Light Company, Renton, Wash.
Raddin, E. H., chief engineer, Champion Lamp Works, Lynn, Mass.
Ratz, E. G., electrical engineer, Canadian Westinghouse Company, Hamilton, Ont., Canada.
Reich, H. J., professor of electrical engineering, University of Illinois, Urbana, Ill.
Thoms, E. G., plant extension engineer, Indiana Bell Telephone Company, Indianapolis, Ind.

20 to grade of Member

Applications for Election

Applications have been received at headquarters from the following candidates for election to membership in the Institute. Names of applicants in the United States and Canada are arranged by geographical District. If the applicant has applied for direct admission to a grade higher than Associate, the grade follows immediately after the name. Any member objecting to the election of any of these candidates should so inform the national secretary before December 31, 1942, or February 28, 1943, if the applicant resides outside of the United States or Canada.

United States and Canada

1. North Eastern

Erben, H. V. (Member), General Electric Company, Schenectady, N. Y.
Fogarty, J. B., United States Torpedo Station, Newport, R. I.
Hydrick, J. C., Jr., General Electric Company, Lynn, Mass.
Kelley, J. B., General Electric Company, Schenectady, N. Y.
MacCrehan, W. A., Jr., General Electric Company, Lynn, Mass.
Nelson, P. H., University of Connecticut, Storrs, Conn.
Ruggles, J. S., New York Power and Light Corporation, Troy, N. Y.
Schlotterbeck, R. S., General Electric Company, West Lynn, Mass.
Seder, L. A., General Electric Company, Lynn, Mass.
Stroberg, E. A. (Member), Stone and Webster Engineering Corporation, Boston, Mass.
Thomson, E. C., Photoswitch Incorporated, Lawrence, Mass.
Tracy, C. A., Westcott and Mapes, Incorporated, New Haven, Conn.
Vindsberg, S. B., Stone and Webster Engineering Corporation, Boston, Mass.

2. Middle Eastern

Akers, M. K. (Member), George Washington University, Washington, D. C.
Bean, T. H., Leeds and Northrup Company, Philadelphia, Pa.
Brennan, F. E., Consolidated Gas and Electric Company, Baltimore, Md.
Crawford, J. O., Bell Telephone Company, Ardmore, Pa.
Danser, J. W., United States Naval Academy, Annapolis, Md.
Crevensten, H. A., Baltimore Transit Company, Baltimore, Md.
Dodd, W. E., Line Material Company, Zanesville, Ohio.
Gard, J. D., Westinghouse Electric and Manufacturing Company, E. Pittsburgh, Pa.
Gilley, J. L., Consolidated Gas and Electric Light and Power Company, Baltimore, Md.
Hartley, R. A., Westinghouse Electric and Manufacturing Company, E. Pittsburgh, Pa.
Hawkeswood, A. E., British Admiralty Delegation, Washington, D. C.
Hecklinger, R. S., Consolidated Gas and Electric Company, Baltimore, Md.
Horstman, W. F., General Electric Company, York, Pa.
Hudak, J. L., Ohio Injector Company, Wadsworth, Ohio.
Klein, G. G., Baltimore Transit Company, Baltimore, Md.
Marsh, R. A., New York Shipbuilding Corporation, Camden, N. J.
Mayer, W. S., Jr., United States Signal Corps, Philadelphia Signal Depot, Philadelphia, Pa.
Moses, G. L. (Associate re-election), Westinghouse Electric and Manufacturing Company, E. Pittsburgh, Pa.
Oughton, J. R., III, Line Material Company, Zanesville, Ohio.
Payne, J. F., Jr., Leeds and Northrup Company, Philadelphia, Pa.
Ripley, C. S. (Associate re-election), 3030 Euclid Avenue, Cleveland, Ohio.
Smith, W. L., The Electric Controller and Manufacturing Company, Cleveland, Ohio.
Soebbing, T. J., Philadelphia Signal Corps, Procurement District, Philadelphia, Pa.
Tankovich, J. G. (Associate re-election), Line Material Company, Zanesville, Ohio.
Towhey, H. M., Westinghouse Electric and Manufacturing Company, Philadelphia, Pa.
Triche, R. P., Westinghouse Electric and Manufacturing Company, Sharon, Pa.
Vandeventer, D., Leeds and Northrup Company, Philadelphia, Pa.
Von Alven, W. H., Capitol Radio Engineering Institute, Washington, D. C.
Wadlow, F. T. (Member), Prack and Prack and Chester Engineers, Pittsburgh, Pa.
Weinfield, B. S., General Electric Company, Philadelphia, Pa.
Woodward, L. H. L., New York Shipbuilding Corporation, Camden, N. J.
Zwelling, M. I., Line Material Company, Zanesville, Ohio.

3. New York City

Adashko, J. G. (Member), Gielow, Incorporated, New York, N. Y.
Becken, E. D., R.C.A. Communications, Incorporated, New York, N. Y.
Blomberg, K. H. (Member), Ericsson Telephone Sales Corporation, New York, N. Y.
Carver, J. V., Jersey Central Power and Light Company, Morristown, N. J.
Coe, W. H., Eclipse-Pioneer Division Bendix Aviation Corporation, Bendix, N. J.
Davis, E. R., Eclipse Aviation Division, Bendix, N. J.
De Marrais, R. J. (Member), Wright Aeronautical Corporation, Paterson, N. J.
Donaldson, W. H., Bendix Aviation Corporation, Bendix, N. J.
Einsiedler, C. S., K. W. Battery Company, Incorporated, Brooklyn, N. Y.
Flagg, H. J. (Member), General Cable Corporation, Perth Amboy, N. J.
Fort, A. C., Jersey Central Power and Light Company, Morristown, N. J.
Haines, W. T., Bell Telephone Laboratories, Incorporated, New York, N. Y.
Harris, J. R., Bell Telephone Laboratories, Incorporated, New York, N. Y.
Heggy, J. M. (Member), Westinghouse Electric and Manufacturing Company, Newark, N. J.
Hunter, W. (Member), Ebasco Services Incorporated, New York, N. Y.
Jorgensen, H., Postal Telegraph-Cable Company, New York, N. Y.
Kiimalehto, E., New York Telephone Company, New York, N. Y.
Kirk, D. (Member), The J. G. White Engineering Corporation, New York, N. Y.
Kirkpatrick, W. T., Kingston-Conley Electric Company, North Plainfield, N. J.
McAvoy, F. E., General Bronze Corporation, Long Island City, N. Y.
Miller, R. F., Gielow, Incorporated, New York, N. Y.
Nilan, J. J., Sr., Consolidated Telegraph and Electrical Subway Company, New York, N. Y.
Plotkin, J. (Associate re-election), Burns and Roe, Incorporated, New York, N. Y.
St. Andre, J. W. (Member), Wright Aeronautical, Plant #7, Paterson, N. J.
Schaefer, J. A., General Electric Company, Bloomfield, N. J.
Straussman, A. D., Long Island Lighting Company, Mineola, N. Y.
Van Auken, O. W., Jersey Central Power and Light Company, Morristown, N. J.
Van Hamel, T. A. (Member), The J. G. White Engineering Corporation, New York, N. Y.
Weiss, L., Federal Telephone and Radio Manufacturing Corporation, East Newark, N. J.
Welter, F. J., Consolidated Edison Company of New York Incorporated, East River Station, New York, N. Y.
Wendt, R. L., Sperry Gyroscope Company, Brooklyn, N. Y.

4. Southern

Blumburg, A. O., Alabama Dry Dock and Shipbuilding Company, Mobile, Ala.
Crisman, J. A., Westinghouse Electric and Manufacturing Company, Memphis, Tenn.
Drawe, W. J., Jr., New Orleans Public Service Incorporated, New Orleans, La.
Gwinner, H. A., American Telephone and Telegraph Company, Birmingham, Ala.
Hayes, A. C., Tampa Electric Company, Tampa, Fla.
Humbert, W. F. (Associate re-election), Marshall Field and Company, Spray, No. Car.
Kammer, K. P., New Orleans Public Service Incorporated, New Orleans, La.
Kittrell, L. W., Southwestern Gas and Electric Company, Shreveport, La.
Levy, M. L. (Member), Higgins Industries, Incorporated, New Orleans, La.
O'Clare, A. R. (Associate re-election), Miami School of Aeronautics, Miami, Fla.
Partain, W. L., Fraser-Brace Engineering Company, Incorporated, Hogansville, Ga.
Trouard, S. E. (Member), New Orleans Public Service Incorporated, New Orleans, La.
Wallisch, C. J. (Member), Tennessee Valley Authority, Knoxville, Tenn.

5. Great Lakes

Barnes, H. O., Sangamo Electric Company, Springfield, Ill.
Burnett, J. E., Westinghouse Electric and Manufacturing Company, Detroit, Mich.
Dumont, D. P. (Member), Indiana General Service Company, Marion, Ind.
Eckel, A., McGraw-Hill Publishing Company, Incorporated, Chicago, Ill.
Foscue, E. S., General Electric Company, Ft. Wayne, Ind.
Foster, S. L., Allis-Chalmers Manufacturing Company, Milwaukee, Wis.
Iglehart, M. M. (Member), Cutler-Hammer, Incorporated, Milwaukee, Wis.
Jacobs, H. S., Harnischfeger Corporation, Milwaukee, Wis.
Kasak, A. J. (Member), Bendix Aviation, South Bend, Ind.
Kober, C. F., Defoe Shipbuilding Company, Bay City, Mich.
Kuzela, J. F., Jr., Line Material Company, South Milwaukee, Wis.
Lithgow, E., Square D Company, Milwaukee, Wis.
Martin, J. S. (Member), Sangamo Electric Company, Springfield, Ill.
Osborn, M. W., Allis-Chalmers Manufacturing Company, Milwaukee, Wis.
Patti, F. D., The Austin Company, Midland, Mich.
Rook, W. E., American Telephone and Telegraph Company, Chicago, Ill.
Schaefer, M. W., Allis-Chalmers Manufacturing Company, West Allis, Wis.
Slaybaugh, E. O. (Member), Consumers Power Company, Battle Creek, Mich.
Swinehart, M. R., Cutler-Hammer, Incorporated, Wilwaukee, Wis.
Tucker, J. G. (Member), Commonwealth Edison Company, Chicago, Ill.
Vogel, R., Square D Company, Milwaukee, Wis.
Wrentmore, L. L., Westinghouse Electric and Manufacturing Company, Chicago, Ill.

6. North Central

Pollock, P. R., Allis-Chalmers Manufacturing Company, Denver, Colo.

7. South West

Acosta, R., Rural Electrification Administration, St. Louis, Mo.
Auten, R. S., Signal Corps, United States Army, San Antonio, Texas.
Berry, J. S., I. A. Bennett and Company, St. Louis, Mo.
Brandt, W. T., Sr., Pennsylvania Shipyards, Incorporated, Beaumont, Tex.
Carpenter, H. E., Oklahoma Gas and Electric Company, Oklahoma City, Okla.
Cerna, H., Rural Electrification Administration, St. Louis, Mo.
Church, R. A., Universi y of Oklahoma, Norman, Okla.
Del Ponte, F., Rural Elec rification Administration, St. Louis, Mo.
Freebersyser, R. W., Westinghouse Electric and Manufacturing Company, St. Louis, Miss.
Gamp, L. H., Jr. (Member), Gamp Electric Company, St. Louis, Mo.
Gil, M., Rural Electrification Administration, St. Louis, Mo.
Grace, W. J. (Member), 7 Branard Court North, Wilshire Village, Houston, Tex.
Harmon, R. C., U. S. Signal Corps, University of Kansas, Lawrence, Kan.
Ladd, R., Rural Electrification Administration, St. Louis, Mo.
Rigby, W. S., Wagner Electric Corporation, St. Louis, Mo.
Rivas, J., Rural Electrification Administration, St. Louis, Mo.
Romero, F., Rural Electrification Administration, St. Louis, Mo.
Schroeder, F. W., Sr. (Member), West Texas Utilities Company, Abilene, Tex.
Stueve, W. H. (Member), Oklahoma Gas and Electric Company, Oklahoma City, Okla.
Williams, H. J., Missouri Electric Power Company, Marshfield, Mo.

8. Pacific

Crosby, R. E., Bethlehem Steel Company, San Francisco, Calif.
Hendrickson, J. A., Pacific Electric Manufacturing Corporation, San Francisco, Calif.
Hong, K. S., Moore Dry Dock Company, Oakland, Calif.
Koller, R., Pacific Electric Manufacturing Corporation, San Francisco, Calif.
Meigs, J. R., University of Southern California, Los Angeles, Calif.
Rose, E. A., 1686 Scenic Avenue, Berkeley, Calif.
Williams, P. H., 2736 Center Street, Bakersfield, Calif.

9. North West

Flechsig, A. J. (Member), The Austin Company, Naval Air Station, Sand Point, Seattle, Wash.
McLean, K. J., General Electric Company, Seattle, Wash.
Stroman, R. E., Mountain States Telephone and Telegraph Company, Fort Peck, Mont.

10. Canada

MacKay, T. A., Hamilton Hydro-Electric System, Hamilton, Ont., Canada.
Squire, E. T., Canadian (British Columbia) Telephone and Supplies Limited, Vancouver, B. C., Canada.
Townson, C. H., Railway and Power Engineering Corporation, Toronto, Ont.

Total, United States and Canada, 145

Elsewhere

Cook, J. E. (Associate re-election), Cia Colombiana de Electricidad, Aptdo. Aereo 532, Santa Marta, Colombia, S. A.
Dreher, C. E., Westinghouse Electric Company of Brazil, Rio de Janeiro, Brazil, S. A.
Hournon, B. A., Westinghouse Electric International Company, Buenos Aires, Argentina, S. A.
Loewel, R. M., Cia Argentina de Electricidad, Buenos Aires, Argentina, S. A.

Total, elsewhere, 4

OF CURRENT INTEREST

War Problems Discussed at Tenth Annual Meeting of ECPD

War problems and the effects of the war on the work of the Engineers' Council for Professional Development held the spotlight at the tenth annual meeting of the Council held in New York, N. Y., October 18, 1942. Among important actions taken were the adoption of a plan of operation for carrying out ECPD functions locally, proposed by AIEE representative J. F. Fairman (F '25), vice-president, Consolidated Edison Company of New York (N. Y.), Inc., and outlined previously at the AIEE 1942 summer convention conference of officers, delegates, and members held in Chicago, Ill., in June 1942.

Canons of ethics for engineers, prepared by a special committee on principles of engineering ethics, were approved by the ECPD for submission to the boards of directors of the constituent societies for consideration and comment. It was announced also that a series of resolutions pertaining to the war man-power problem had been adopted by the ECPD executive committee at its meeting of September 20, 1942 (*EE, November '42, pp. 583–4*).

A new special ECPD committee on unionization of engineers was authorized to study the union problem as it relates to engineers and report at a future meeting. Upon recommendation by the committee on professional training, the publication of a "Manual for Junior Engineers," which has been under preparation during the past year by that committee, was authorized. Also reports were presented by a representative of each of the eight constituent societies of ECPD.

On recommendation of the committee on engineering schools, ECPD accreditation of curricula was granted as follows:

University of Notre Dame: aeronautical, civil, electrical, mechanical, and metallurgical engineering.

University of Toledo: course in general engineering.

Bucknell University: chemical engineering.

Catholic University of America: mechanical engineering.

Cooper Union Night School of Engineering: chemical engineering.

University of Florida: chemical engineering.

Georgia School of Technology: ceramic engineering.

University of Idaho: geological engineering.

University of Maryland: chemical engineering.

Northeastern University: chemical engineering.

Oregon State College: chemical engineering.

Texas Agricultural and Mechanical College: aeronautical engineering.

University of Southern California: civil, electrical, and mechanical engineering.

Worcester Polytechnic Institute: chemical engineering.

A total of 577 engineering curricula in 131 colleges and universities in the United States have been accredited by ECPD through the committee's inspection program during the last decade. Only degree-granting colleges are eligible to be accredited by ECPD.

LOCAL ACTIVITIES TO DEVELOP ECPD AIMS

The plan, promulgated by J. F. Fairman, for carrying out locally the purposes of ECPD provides for the establishment of programs in local sections or chapters of constituent societies to bring about an understanding of ECPD objectives and an active interest in them (*EE, August '42, pp. 419–21*). Although many have thought that ECPD's most significant accomplishment has been its accrediting work, the organization has other aims, no less significant, toward which its work must be shaped—selection and guidance, professional training, and professional recognition.

It is suggested that local sections or chapters of member societies might best become active in promoting work on selection and guidance patterned along the lines of procedures already developed in certain metropolitan areas in connection with the work of the ECPD committee on student guidance. This committee endeavors to develop means for the educational and vocational orientation of young men as to the responsibilities of the engineer and opportunities in engineering, in order that only those who have the high-quality aptitude and capacity required of engineers seek to enter the profession. Co-operation with local high schools in conducting vocational counseling work with prospective students and in planning programs to stimulate interest of parents and students in engineering is urged.

In enlarging the scope of work in professional training aimed at assisting young graduates to become oriented and active in their own professional development, a manual designed for the guidance of the young engineer during his formal educational period and during the ensuing apprenticeship is now in progress. The ECPD committee on professional training also has arranged to make engineering periodicals available to members of constituent societies who are in the Armed Services by having the publications sent to regularly established libraries (*EE, July '42, p. 359*).

Attempts to encourage co-operation of local engineering groups with one another and with high-school and college officials in this program are emphasized inasmuch as ECPD itself is not organized to enter the local field. However, committees of ECPD will give assistance by answering questions, by supplying information as to the experiences of other local groups, and by making suggestions based on their observations.

Officers and Committees

ECPD officers elected for 1942–43 are:

R. E. Doherty, president of Carnegie Institute of Technology, Pittsburgh, Pa., *chairman* (re-election); S. D. Kirkpatrick, editor, *Chemical and Metallurgical Engineering*, New York, N. Y., *vice-chairman;* A. B. Parsons, secretary, American Institute of Mining and Metallurgical Engineers, *secretary;* S. L. Tyler, secretary, American Institute of Chemical Engineers, New York, N. Y., *assistant secretary.*

Members of the executive committee are:

R. E. Doherty, chairman ex officio; S. D. Kirkpatrick, vice-chairman ex officio; W. B. Plank, professor of mining, Lafayette College, Easton, Pa., AIME; J. F. Fairman, assistant vice-president, Consolidated Edison Company of New York (N. Y.), Inc., AIEE; A. R. Stevenson, Jr., staff assistant to vice-president in charge of engineering, General Electric Company, Schenectady, N. Y., ASME; B. F. Dodge, professor and chairman of department of chemical engineering, Yale University, New Haven, Conn., AIChE; C. C. Williams, president, Lehigh University, Bethlehem, Pa., SPEE; J. B. Challies, vice-president and executive engineer, Shawinigan Water and Power Company, Montreal, Que., EIC; ASCE and NCSBEE to be announced.

The following representatives of ECPD member societies were announced for the 1942–45 term:

Reappointments—G. W. Burpee, consulting engineer, Coverdale and Colpitts, New York, N. Y., ASCE; R. L. Sackett, dean emeritus of engineering, Pennsylvania State College, ASME; O. W. Eshbach, dean Northwestern Technological Institute, Northwestern University, Evanston, Ill., AIEE; C. M. A. Stine, vice-president, E. I. duPont deNemours and Company, Wilmington, Del., AIChE; Arthur Surveyer, consulting engineer, Montreal, Que., EIC.

New appointments—A. F. Greaves-Walker, professor of ceramic engineering, North Carolina State College of Agriculture and Engineering, Raleigh, AIME; D. B. Prentice, president, Rose Polytechnic Institute, Terre Haute, Ind., SPEE.

Newly elected committee chairmen for 1942–43 are:

Committee on student selection and guidance, A. R. Cullimore, president, Newark (N. J.) College of Engineering.

Committee on information, E. H. Robie, editor, American Institute of Mining and Metallurgical Engineers.

The following committee chairmen were re-elected for the coming year:

Committee on engineering schools, D. B. Prentice, president, Rose Polytechnic Institute, Terre Haute, Ind.

Committee on professional training, E. S. Lee, engineer, general engineering laboratory, General Electric Company, Schenectady, N. Y.

Committee on professional recognition, C. F. Scott, professor emeritus of electrical engineering, Yale University, New Haven, Conn.

Report of Chairman Doherty

"During the current year ECPD has not escaped the pressures of war, and the

prospects are that it will feel them still more," began Chairmen Robert E. Doherty in his annual report to ECPD. "However," he continued, "the work of the Council has gone forward with thoughtful and, I hope, wise adaptation to the war pressures. Despite the outlook immediately after Pearl Harbor that ECPD would probably be obliged to curtail greatly its several activities, the actual trend has been the opposite."

Concerning future activities of ECPD, Chairman Doherty expressed the view that "the Council should take an aggressive position. It should pursue at an accelerated rate every activity within its charter that gives promise of supporting the war effort or that would lay foundations of professional development that may now appear to be essential for the most effective service to the country by engineers and the engineering profession in peace-time reconstruction. And as I now briefly scan the several areas of the ECPD landscape I shall comment specifically where I think especial emphasis should be laid, and this procedure will indicate more definitely the policy I would recommend.

"In the first place I wish to say a word concerning the question raised by the following action of the executive committee at its June 8, 1942, meeting:

VOTED that it is the consensus of the executive committee that publicity and other activities of ECPD be directed toward the professional development of the individual engineer for a greater effectiveness in dealing with social and economic problems, as well as with technical problems.

"Singularly enough the original purpose of this vote was to expand, not contract, the scope of activity to include professional development in social and economic, as well as in technical problems. This enlarged scope is authorized specifically in the charter. Discussion of the matter at the meeting brought out the consensus that in carrying out the enlarged purpose, activities should be directed toward the professional development of the individual engineer. . . ."

Referring to earlier years, and discussing methods by which ECPD might reach the individual, Chairman Doherty said in part: "There are two general purposes which I would recall. Two years ago it seemed clear that one of the ways in which ECPD could best promote professional development was to cultivate more fully among the constituent bodies an attitude of co-operation and an improved practical facility in taking joint action. There was of course no thought of joint action on matters outside the scope of ECPD, or even on untimely matters within the scope. Simply, co-operation in ECPD matters would presumably make it easier to co-operate in others. If so, an advance would be made in professional development. While there is here no dramatic advance to report, I am certainly not discouraged with the progress and wish to emphasize that, in my opinion, this end is no less important today than it was two years ago.

"The other purpose was an educational campaign. The objectives of ECPD could never be achieved if the members of the Council, or perhaps even only the members of the executive committee, knew what ECPD was and what it was trying to do. Individual engineers must know and be interested in these activities. Hence every effort was to be made to inform the individuals among the boards of direction and the memberships of the several constituent bodies—indeed all engineers—of the purposes and activities of ECPD and to encourage them to have an active interest. As you all probably know, substantial progress has been made in this connection, and here also I urge that the matter is no less important than it was two years ago.

"Machinery for reaching the individuals was missing, but this has been planned. Indeed its essential elements already existed; it needed only to be defined and put in motion. As to reaching the individual members of the boards of direction, the three representatives of each of the several constituent bodies were the potential, but inactive, liaison between the Council and the board members. This liaison has begun to be effective, and the reports of these delegations at the annual meeting represent the two-way flow of this plan. And, needless to say, the secretaries of the constituent bodies are most helpful in keeping this part of the machinery oiled.

"The problem of reaching individual engineers generally is somewhat more complicated. Fortunately, however, in this case also, most of the main elements of necessary machinery are in existence. The channels of flow from the Council to such individuals are the national organizations of the constituent bodies. In all major cities and industrial centers there are local branches or sections of the national organizations; and on the campuses of most engineering schools there are student branches, and on some of them, branches of the Society for the Promotion of Engineering Education also. There should of course be SPEE branches on all such campuses and a move to this end should be encouraged. It remains to organize at such centers where the organizations do not already exist joint groups in which activities relating to professional development can be centered, and which, through local branches or sections, can be in communication with ECPD through the national organizations. Such a plan, it seems, is essential to the Council's work, and Mr. Fairman and the chairman have undertaken the formulation of a detailed proposed plan. Here again, I would add, the sympathetic help of the several secretaries is absolutely essential."

Chairman Doherty concluded his report with a discussion of the financial situation of ECPD, which he characterized as sound, and the work of the ECPD standing committees. Excerpts from the reports of these four committees follow.

ECPD Committee Reports

STUDENT SELECTION AND GUIDANCE

A summary of ten years' work by the committee on student selection and guidance was presented by R. L. Sackett, dean emeritus of engineering, The Pennsylvania State College, State College. Dean Sackett retired from the chairmanship of this committee after having served for the entire ten-year period since ECPD was first organized. "A review of the reports submitted by this committee reveals that three fields of action have been attempted," Dean Sackett reported. "The first was exploration. We found education and psychology could give little light on the aptitudes which differentiated the engineer. The committee gave the co-operative test service examinations in mathematics and English to nearly 2,000 entering freshmen engineers. The conclusion that mathematics was one criterion of engineering ability, was clear. The English test showed a lower correlation but when analyzed into its parts of spelling, vocabulary, and usage, it was observed that usage of words was a better criterion than the result of the entire test."

Calling "guidance by engineers" the second effort of the committee, Dean Sackett outlined the early efforts in that direction and discussed the organization of advisory committees of engineers. "Such committees have been organized through the interest of individuals in city and state engineering societies and by appeal to local sections of national engineering societies," the report stated. "Whenever the need is impressed on local sections the response has been excellent but the most significant promotional work remains to be done with the local sections." The report also reviewed results achieved by advisory committees in various localities, and called attention to the "Manual for Committees or Engineers Who Aid Young Men Interested in Engineering Education and the Engineering Profession," which had been produced in printed form during the past year.

"The third enterprise and an important one," the report continued, "depended on the interest of President A. R. Cullimore of the Newark (N. J.) College of Engineering [and newly appointed chairman of the committee on student selection and guidance] in the objectives of this committee. Through him Doctor Russell S. Bartlett was loaned to the committee, and a corresponding committee of the SPEE, to carry on a study of tests and especially to apply a battery of tests designed to show whether the engineer was different or just run of mine in his interests and aptitudes. Dr. Bartlett was familiar with the extended psychological studies made under the direction of Dr. A. B. Crawford at Yale University for mental differences between arts, science, and engineering students.

"Arrangements were made to give such a battery to arts and engineering freshmen at the opening of institutions in the fall of 1941. The following participated: The Universities of Florida, Missouri, Tennessee, Texas, Northwestern University, and Pasadena Junior College. A set of six tests was administered to about 2,000 entering freshmen in arts and engineering.

"The results are embodied in a report to be published in the *Journal of Engineering Education* at an early date. It is too long to do more than summarize it here. The results indicate that differences are ex-

posed by such tests. In general the engineering student who rates high in mathematics, spatial visualization, and mechanical aptitudes is a good prospect for engineering or science education. His scores may not be as high in verbal comprehension, artificial language, or verbal reasoning as the typical arts student who will normally stand lower in mathematics, spatial relations, and mechanical aptitude. As a matter of fact, the graphs or profiles of achievement in such a battery range all the way from those few students who make high scores in all subjects down to those who do so poorly in all six subjects that they indicate poor preparation for any of the usual academic fields.

"A slightly modified battery of tests of the same general content was given to about 1,000 high-school juniors resident in or near Newark, N. J. Of the results Doctor Bartlett says, 'Evidence that the tests are measures of aptitude is found in the comparable range of scores of the two groups, college and high school, in most cases two years apart in academic age.'

"If supported by further experiments, and the study should be continued, the report sets a definite milepost of advance in test programs. We have no evidence of a corresponding differential study having been made heretofore. It is significant as an achievement and would justify the efforts of the committee to make a contribution to the selection and guidance of engineering students."

ENGINEERING SCHOOLS

In the report of the ECPD committee on engineering schools, D. B. Prentice, president, Rose Polytechnic Institute, Terre Haute, Ind., chairman, reviewed the work of the committee during the past year and outlined the basic policies of the committee's accrediting program. "During the current year," the report stated, "visits were made to 8 institutions for the purpose of reinspecting 35 curricula on the accredited list, to 11 institutions with one or more accredited curricula to appraise 15 nonaccredited curricula, and to two institutions on the accredited list to examine six curricula." The status of the accrediting program as of October 1942 is indicated in Tables I and II.

The committee's plans during the war period call for curtailment of the accrediting program as indicated by the following paragraphs: "Many members of engineering-college faculties are carrying additional responsibilities during the war as consultants or teachers of Engineering, Science and Management Defense Training courses. Some engineering departments have temporarily lost important staff personnel to military service or research. Preparation for an inspection or reinspection by a delegatory committee of the committee on engineering schools is an obligation which in many instances it is possible and safe to postpone; also members of the delegatory committees often find it difficult to arrange for even short inspection trips.

"For the above reason the committee on engineering schools proposes to defer many

Table I. Status of the Accrediting Program by Curricula

As of October 18, 1942, Not Including Options Under Accredited Curricula

Curricula	Totals
Fully accredited	495
Provisionally accredited	74
Not accredited	103
Action pending	9
Total	681

of the visits to institutions for the examination of curricula which would normally be conducted during the next year. The existing accreditations, pending reinspections, will be continued. Exceptions to this plan will be made in cases which seem to regional chairmen to need attention or when postponement of an inspection would be unjust to a college.

"The curtailment of the inspection program notwithstanding, the committee on engineering schools feels that strict attention should be given during the war years to the end that college educational standards are not allowed to slip or facilities run down.

"There are an ever increasing number of war measures that bring external pressure to bear on now war-tensed engineering faculties and student bodies. The expedited programs, the raids on faculties and student bodies, and the ESMDT are perhaps the most apparent. The committee believes that although it is too early to draw any definite conclusions, the situation merits continued attention and data should be accumulated to determine what action can be recommended in the future.

"One of the major problems of the war effort centers around the proper technical training for war production positions. The committee proposes that wherever in critical areas the War Manpower Commission or the Selective Service System requests its aid in determining the appropriateness of certain training programs as being 'training for essential industries' and therefore justifying military deferment, this committee is willing to assist through the formation of area or regional engineering war manpower committees."

PROFESSIONAL TRAINING

"The abilities during the past few years of the ECPD committee on professional training have been applied to surveying the present opportunities for the professional training of the younger engineers," stated the report of this committee. Everett S. Lee (F '30), engineer, general engineering laboratory, General Electric Company, Schenectady, N. Y., is chairman of the main committee, and John T. Sherman, American Cyanamid Company, New York, N. Y., is chairman of the junior committee. The preparation of a 'Manual for Junior Engineers' was reported to be the committee's principal activity during the past year. Progress on this project is indicated in the following paragraphs from the report.

"The recommendations of the committee on professional training last year stressed the desirability of a close co-ordination between the agencies involved in the professional training of the younger engineer. To this end the desirability of a 'Manual for Junior Engineers' has been suggested, and the start of the work involved in the writing of such a manual has been the major activity of the committee for this past year.

"In the past the generally basic thought has been that when the engineering student graduates he leaves school and goes into active practice, which is largely in industry, in teaching, or in other varied forms.

"The manual would rather progress the thought that in the undergraduate course the student is building a foundation for his later professional life to include both his technical and his social contributions, and thus he should undertake to lay out a program for himself to insure progressive improvement in a well-rounded life.

"To be successful this thought must be basic on the part of the student, on the part of industry, and on the part of the engineering schools—on the student that he recognize it and pursue it; on industry that the young engineer is made conscious of an interest in him by what he finds there; and on the schools that they plant the seed in the undergraduate days and inspire and provide for a continuance of opportunity for advancement in both specific and general knowledge. The student would thus be graduated with thoughts of continued self-improvement, and wherever he went, he would find a proper climate in which to raise the planted seed.

"The 'Manual for Junior Engineers' would bring to all an understanding of this mutually accepted thought—to the student, to industry, to the engineering schools—and would provide a tangible means for progressing it."

Table II. Status of the Accrediting Program by Institutions

As of October 18, 1942

Approximate number of engineering degree-granting institutions in the United States	167
Institutions inspected (not including reinspections)	143
Institutions having one or more curricula accredited	132
Institutions inspected but having no curricula accredited	9
Institutions reinspected but having no curricula accredited	2
Total number of institutions acted upon	143
Institutions applying but inspection deferred	5
Institutions that have not applied	19

Regarding the effect of the war on the committee's activities, the report concluded: "The war has naturally prevented the normal amount of time being given to the work of the committee. Several of the members have entered into the Service. But on the part of those remaining, there is the definite conviction that helpful service may be rendered in the future by such application as is possible towards the attainment of our stated objectives, and thus, all possible application is being made. The writing of the 'Manual for Junior Engineers' will not be an easy task, but such assembling of a proper statement for guidance of the junior engineer and his associates should be worth many times the effort."

PROFESSIONAL RECOGNITION

Early ambitions of the ECPD committee on professional recognition, of which Charles F. Scott (HM '29), professor of electrical engineering emeritus, Yale University, New Haven, Conn., is chairman were reviewed in the annual report of that committee. The report continued with a review of the collapse of the early program and of the committee's fresh start in 1938. Principal activities reported for the year 1941–42 center around means for actually making students aware of their profession. The means pursued during the year comprised the publication and dissemination of an address by William E. Wickenden, president, Case School of Applied Science, Cleveland, Ohio, entitled "The Second Mile." Doctor Wickenden's address was delivered originally at a meeting of the Engineering Institute of Canada and was subsequently published in slightly revised form in *Electrical Engineering* (*May '42, pp. 242–7*) and in other society publications. Under sponsorship of the ECPD committee on professional recognition, the address was reprinted from *Electrical Engineering*, and the committee reported that more than 10,000 copies had been sold between April 15 and September 15, 1942.

"The address is not a handbook," the report stated; "It has no prescription for raising salaries; it deals with ideals; it starts off with a text like a sermon; it is a sermon, in applying a great truth to present-day life; it makes 'motivation of the individual' the true basis of professional life."

The report states that the committee's job now is "to drive in the wedge so well started; to promote understanding of the Wickenden address as indicating ways 'in which the professional stature of the engineer may be developed, and engineering in its broadest sense may be advanced.' "

ECPD Dinner Meeting

The dinner meeting held in connection with the ECPD annual meeting was attended by 33 officers, committee members, and representatives of the constituent societies. Colonel C. E. Davies, secretary, American Society of Mechanical Engineers, now serving with the Ordnance Department, Washington, D. C., acted as toastmaster. Speakers of the evening included chairmen and members of ECPD's committees, who spoke on the work of the respective committees; L. Austin Wright, general secretary, The Engineering Institute of Canada, and deputy director of Canadian Selective Service; Webster N. Jones, chairman of a committee to determine the manner in which the office of technical development should be set up within the War Production Board; and others.

In his preliminary remarks, Colonel Davies declared that we have not even started to establish engineering as a profession, so far as the public is concerned. He emphasized the importance of co-operative action by the various engineering groups in working toward this end, and pointed to the possibilities of co-operative action in this direction through ECPD. He declared that ECPD must increase the rate of growth of quality of engineering in order that the quality of engineering as a profession may be raised.

Speaking for the committee on professional recognition, C. F. Scott, chairman, characterized certification as the first step for young men entering the profession toward recognition as engineers. This, of course, requires adequate preparation. In striving for higher professional standards, engineers should work toward greater efficiency in handling technical problems, and then apply their talents to economic and social aspects of engineering problems. Doctor Scott outlined the history of the ECPD recognition program and discussed the activities of the committee for the past year. In conclusion, Doctor Scott said that engineering is like the church: It has many branches just as the church has many denominations, but the *spirit* of engineering is common to all branches. In this connection he compared ECPD to the Federal Council of Churches and pointed to similar opportunities for co-operative work.

Chairman D. C. Jackson of the committee on principles of engineering ethics, briefly reviewed the work of his committee. He first referred to the problems encountered in early efforts of the AIEE to formulate and adopt a code of ethics and related some of the early difficulties in bringing together the widely divergent views on the subject. Doctor Jackson stated that the same types of difficulties were encountered while working up the code that the committee prepared during the past year. He declared that such a code is of first importance in establishing the engineering profession in the minds of the public, stating emphatically that we must have "canons of ethics" before we can expect the public to recognize engineering as a profession.

Speaking for the committee on professional training, S. B. Kirkpatrick, member of that committee, first pointed out that the young engineer's growth levels off during the first few years after graduation from college and that professional training is needed most during these years. He reviewed briefly the accomplishments of the committee under previous chairmen, General R. I. Rees and O. W. Eshbach (F '37). He emphasized the necessity for the work of the committee to be carried on down below the "brass hat" level in order to be most effective. He expressed the opinion that the "Manual for Junior Engineers" now under preparation by the committee would prove to be of great assistance in this effort.

Three stages in the work of the committee on engineering schools were discussed by D. B. Prentice, chairman. The first stage comprised preparation of criteria to form the basis of the curricula accrediting program that has been the committee's principal accomplishment; this program then was carried out on a trial basis in the New England territory. The second stage comprised the extension of accrediting activities from the New England area to the remainder of the United States. In discussing this stage of the committee's work, Mr. Prentice outlined the procedure followed and emphasized the importance of accrediting by individual curricula. Having established standards for accrediting, Chairman Prentice discussed briefly the third state of the committee's work—the maintenance of those standards. He emphasized the importance of this stage, upon which the committee is just entering, and also the difficulties that may be encountered under wartime conditions.

Progress made by the committee on student selection and guidance during the ten years of ECPD's existence was reviewed by retiring chairman R. L. Sackett. He contrasted the early indifference of the engineering profession to selection and guidance work to the strong support that the program now enjoys. Originally, there seemed to be a feeling of satisfaction in allowing graduates of secondary schools to enter engineering colleges, leaving to the colleges the necessary "weeding out" process. There is now recognition of the fact that this is undesirable from two points of view: It is discouraging to the students who prove to be misfits in engineering colleges and must undergo the stigma of failure in that endeavor; and it imposes an unnecessary and undesirable burden on the engineering colleges by causing them to spend time training students who later prove to be misfits. Dean Sackett then reviewed briefly the experience of the committee with aptitude tests and pointed to the favorable results obtained during recent years.

TRIBUTE TO DEAN SACKETT

At the conclusion of Dean Sackett's remarks, Editor George A. Stetson of the American Society of Mechanical Engineers read a prepared tribute to Dean Sackett in recognition of his ten years' chairmanship of the ECPD committee on student selection and guidance. A portion of Mr. Stetson's statement follows:

"Long years of service in the field of engineering education have matured his wisdom and judgment. Experience with several generations of youth, with practicing engineers, and with the men and women of this nation has been his in rich measure. Some inner quality of alertness to men and events has kept his mind youthful and his vision clear. Although justified by years and the value of his accomplish-

ments in enjoying the leisure that the vigor of his health would make a pleasure rather than a burden, he has preferred to pioneer in the field of selection and guidance of young engineers, and has devoted his time and energy to a new and important aspect of engineering education upon which the quality of future generations of engineers will indubitably depend. To the professional social scientist and the pedagogical psychologist he is a layman; but those who have known him and worked with him recognize that new ideas in any field coalesce into the substance of practicability when exposed to the influence of his maturity and experience.

"Dean Sackett, it is my privilege to extend to you on behalf of this group of your friends and fellow workers their congratulations and their tribute to you for your services in the field of engineering education and particularly for your ten years of leadership in developing the techniques of selection and guidance of engineering students. As your studies bear fruit in application in education and engineering, more and better leaders will be developed and a larger and more intelligent group of young men will rally to wise leadership. You cannot pass on to them your maturity and experience, but, when peace returns, following your example and aided by the influences you have set in motion, they will be able to distinguish between true leadership and false, bring education to serve the liberties and well-being of mankind, and build a nobler profession that will be the master and not the victim of applied science through a wise use of maturity and experience."

SELECTIVE SERVICE IN CANADA

A bird's-eye view of how the Canadian Selective Service system handles technical personnel was presented by L. Austin Wright, deputy director of the Canadian Selective Service. He described the Wartime Bureau of Technical Personnel established by the Canadian Institute of Chemists, the Canadian Institute of Mining and Metallurgy, and the Engineering Institute of Canada. The Bureau is operated as a unit of Selective Service system and is supported by the Canadian Government.

Mr. Wright reported that about 25,000 engineers and scientists had been registered, which represents from 80 to 90 per cent of the total of such men in Canada. Pointing out that Canada has established complete allocation of all man power, he said that no engineer can seek employment without first informing the Bureau of Technical Personnel. Likewise, no employer can hire an engineer without the permission of the Bureau. Employers are required to hold jobs open for employees at the close of the war, whenever the Bureau places an employee in a different job.

Canadian students in engineering and technical courses are allowed to complete their courses in a normal manner (Canada has not yet established accelerated courses), but students granted that privilege must make themselves available for employment by the armed services after graduation. One failure in any scholastic subject cancels the privilege to complete his course and makes him immediately available for service. Regarding postgraduate work, Mr. Wright said that no engineering postgraduate work is being allowed during the remainder of the war, except in special cases.

Under the Canadian complete-allocation plan, high-school students will be conscripted as necessary for the technical colleges in order to assure an adequate supply of technically trained men. Under this plan, high-school students would complete their courses after which they would be placed in technical colleges for regular collegiate training. Upon graduation, such men would be allocated between industry and the armed services to meet current requirements. Mr. Wright mentioned also that all women in Canada have been registered and many are being placed in industrial work and other positions.

WPB Announces Controlled-Materials Plan for War and Civilian Production

A long-range plan for controlling the flow of critical materials into war production—the Controlled Materials Plan—was announced November 2, 1942, by Donald M. Nelson, chairman of the War Production Board.

Evolved from existing distribution systems and from experience gained through their operation, the CMP has the approval of all governmental agencies participating in it. It was drafted after lengthy conferences with the Army, Navy, Maritime Commission and the WPB office of civilian supply as well as representative consumers and producers of materials.

TO SUPPLY MATERIALS WHEN NEEDED

The main purpose of the plan is to make certain that production schedules are adjusted within material supply so that production requirements are met. This will be accomplished by:

1. Adjusting requirements for critical materials to the supply.

2. Making the quantity and type of materials needed available at the time required to meet approved programs.

Allotments of critical materials will be made through seven claimant agencies, such as Army, Navy, office of civilian supply, and so forth, to prime contractors producing essential goods. Prime contractors, in turn, will divide the allotments with their subcontractors and suppliers.

Carbon and alloy steel, copper, and aluminum—the three most basic and critical materials—are the first controlled materials to be directly allotted under the plan, which becomes effective, in its transitory stage, in the second quarter of 1943 and will be in full operation by July 1.

VERTICAL ALLOTMENT

This method of distributing materials is, in effect, "vertical allotment." So far as the CMP is concerned, it will gradually replace the present priority system, including the Production Requirements Plan, which is on a horizontal basis.

Under PRP each firm, large or small, prime contractor or subcontractor, submits his own requirements to the WPB for approval, and receives an individual authorization to obtain materials.

Under the new CMP, prime contractors will prepare and submit a breakdown of all materials required for the approved end products on which they are working. The breakdown will comprise a bill of materials specifying not only what materials are required, but when they must be received to carry out the authorized program.

CLAIMANT AGENCIES

The claimant agencies are: Army, Navy, Maritime Commission, the aircraft scheduling unit, Lend-Lease, Board of Economic Warfare, and office of civilian supply.

Each claimant agency will break down its submission of requirements into materials for: 1. production; 2. construction and facilities; 3. maintenance, repair, and operating supplies. Requirements for construction and facilities, including industrial machinery and equipment, will be channeled through the construction and facilities branch of the office of program determination.

When requirements have been brought into balance with supply and the program of the various claimant agencies are approved, the WPB vice-chairman on program determination—who also is chairman of the requirements committee—will allocate with the advice of the requirements committee, authorized quantities of the three controlled materials to each.

The claimant agencies, in turn, will distribute these broad allotments among prime contractors by means of allotment numbers, which will constitute a right to receive delivery. The prime contractors will pass on the allotment numbers as necessary to their subcontractors and suppliers.

HOW THE PLAN WORKS

Materials other than controlled materials will continue to be distributed through the priorities system. Each company receiving an allotment number carrying an allocation of controlled materials also will receive a preference rating for use in obtaining other materials. A preference rating accompanied by an allotment number will be higher than other ratings of the same category, but will not take precedence over higher ratings. For example, *AA*-3, plus an allotment number, is higher than *AA*-3, without the number, but not as high as *AA*-2*X*. The preference ratings also will

resolve conflicts which might otherwise occur in the production and delivery of manufactured items.

In order that sufficient amounts of materials in the form desired may be available, responsibility for directing the production of controlled materials rests in the controlled materials branches of the WPB. For instance, the iron and steel branch is responsible for steel, and the copper branch for copper. Production Directives, specifying the quantities and forms and shapes of material to be produced during a stated period of time, will be sent to most producers of controlled materials monthly. If orders beyond a specified capacity to produce are received, a producer must refuse them and notify the appropriate controlled-materials branch. If a consumer with an allotment number cannot place his order satisfactorily, he should appeal to and will be assisted by the branch.

As CMP goes into effect, the job of cutting out all nonessential production, military and otherwise, will be completed. Under CMP, each claimant agency will program the quantities of end products—guns, planes, Liberty ships, railroad cars, bedsprings, and so forth—most urgently needed for each quarterly period. From the bills of materials for each of these items the agency will make up a consolidated estimate of its total requirements. These detailed estimates for the second quarter of 1943 must be submitted by January 1, 1943. At the same time, similar estimates must be submitted for the remaining quarter of 1943 and the first quarter of 1944, together with general estimates for the first half of 1944, so that the requirements committee will have at all times a general picture of requirements, eighteen months in advance.

WAR ORDERS GIVEN PREFERENCE

When the allotments are made by the requirements committee, they will be transmitted to prime contractors through the claimant agencies. Manufacturers working on items such as tanks, ships, or aircraft, which generally are contracted for by or through a claimant agency, and are called "Class *A*" products—will receive their allotment with an allotment number directly from the agency. Producers of a list of "Class *B*" products, such as generators, hardware, kitchenware, electrical appliances, parts frequently incorporated in other products, and civilian items generally, will receive their allotments from their WPB industry branches, which in turn will receive allotments through the Office of Civilian Supply.

OVERALLOTMENT

Each claimant agency may allot for each month up to 105 per cent of its monthly allotment. This over allotment is intended to stimulate increased production from producers of controlled materials. Claimant agencies also are authorized to make allotments for future quarters on the basis of declining percentages of allotments established for the current quarter. These percentages are:

For the quarter immediately following the one for which a definite allotment has been made, 80 per cent; for the next following quarter, 60 per cent; for all later quarters, 40 per cent.

The plan will be flexible enough to permit limited amounts of material to be given out without allotment numbers. Special provision, for instance, is made for allotments of controlled materials to warehouses so that they may handle small orders without allotment numbers.

A new form of inventory control is to be established with the requirement that every primary or secondary producer whose inventory of all controlled materials is in excess of a specified amount must submit an inventory statement showing his position at the end of each calendar quarter not more than 15 days later.

TRANSITION PERIOD PROVIDED

A timetable for the transition from existing systems to full operation of the CMP is provided. The first bills of materials will be assembled by the claimant agencies during November and December 1942 and on January 1, 1943, the agencies will submit their first estimate of requirements to the branches handling controlled materials, with copies to the requirements committee.

By January 15, 1943, the controlled materials branches will have analyzed the requirements and made preliminary reconciliation to the extent possible between requirements and supply. At the same time, the claimant agencies and prime consumers will be developing information necessary in making final allotments, to be in readiness for distribution of allotments to them by the requirements committee.

On February 1, 1943, the requirements committee will make allotments of controlled materials to claimant agencies for the second quarter of 1943. During February and early March, 1943, distribution of allotments will be made by claimant agencies to prime consumers, who in turn will divide their allotments with their secondary consumers and suppliers.

By March 15, 1943, users of controlled materials will have placed authorized orders for April delivery and for later months, as authorized. Subsequently the controlled materials branches will watch placement of orders on mills and mills' shipments, and give assistance in placing orders to authorized users of controlled materials who are unable to obtain mill acceptance of authorized orders.

On July 1, 1943 CMP will be in full operation. Until that time existing procedures, including preference rating orders and PRP certificates and individual material allocations under M orders will continue in effect for consumers who have not been able to qualify under CMP.

Those remote secondary consumers who have not obtained their allotments under CMP in time to meet requirements for the second quarter of 1943 will be authorized to continue purchases under PRP equal to their first quarter authorizations.

To prevent duplication each company operating under PRP will be required to cancel authorizations made under PRP in equal amount for CMP allotments and the total authorizations outstanding at any time will not be permitted to exceed available supply. Orders bearing CMP allotment numbers will be given preference at mills over PRP orders and other rated orders not under CMP.

Official copies of a pamphlet outlining the new Controlled Materials Plan are being made available for free and general distribution from WPB field offices.

War Department Merges Specialist Corps and Army Officer Procurement System

As already announced in the news press, the Army Specialist Corps will be discontinued December 31, 1942, by direction of Secretary of War Henry L. Stimson. As previously reported in *Electrical Engineering* (*EE, Aug. '42, pp. 440–2; Sept. '42, p. 490*) the Specialist Corps was brought into being during the spring months of 1942 for the purpose of obtaining from civil life especially qualified technical and professional men for duties with the Army where military training and experience are not essential. The procurement functions and basic policies of the Corps are being absorbed by a new War Department agency which has been designated as the Officer Procurement Service; this new agency also is taking over from the office of the Adjutant General essentially all officer-procurement functions. Thus, the OPS will provide a single consolidated channel for officer-procurement for all branches of the Army.

In general, the requirements for the commissioning of Army officers, including technical and other specialists, have been tightened materially. To be eligible for consideration now, a candidate must be at least 35 years old *and* in Selective Service class III-A, III-B, or certain restricted categories of IV-F, and must have "civilian training and experience for the particular position beyond that normally provided at Officer-Candidate Schools." Exception to these rules is allowable "only where there is critical need for the services of a particular individual or where the individual is within a 'scarce category' of specialized skill in which not enough trained men to fill the requirements of the armed forces are available at the time required." Airplane pilots, radio mechanics, and electronics specialists are typical of the "scarce categories" referred to.

Contrary to widespread misconception, the Army Specialist Corps was not a separate functioning unit of the Army like the Engineer Corps or the Medical Corps.

The ASC of itself was only a procurement service. It could not select and appoint qualified specialists and then assign them to appropriate jobs in the Army structure. The ASC could act to procure the services of a qualified candidate only upon the specific request of one of the branches or services of the Army. Further, the candidate, upon appointment as an ASC officer became a member *not* of the ASC, but of the requesting agency. The only functioning members of ASC, as such, were the headquarters-staff personnel in Washington, D. C., and in the 35 field offices that had been established in collaboration with the Officer Procurement Branch of the Adjutant General's office. These field offices are in process of translation into field offices of OPS.

Subject to the recommendations of the commanding officers of the Army branches to which individual ASC appointees have been assigned, subject to a review of the candidate's physical-examination report by a member of the Surgeon General's staff, and subject to the final and determining action of the Secretary of War's Personnel Board, each such ASC appointee will be tendered a commission in the Army of the United States, if the appointee filed a request for such commission no later than December 1. All appointees from whom requests for AUS commission were not received by OPS by December 1, 1942, will be given an honorable discharge as of December 31, 1942. In similar fashion, members of the ASC headquarters staff will be tendered commissions or discharges according to circumstances. The headquarters staff members are subject to assignment to other branches of the service as the need for their services in headquarters diminishes with the perfection of the consolidation.

By order of the Secretary, ASC appointees in service or ordered to active duty by October 31 are subject to the procedure just outlined, and are eligible for conversion into AUS officers "at the same rank or one rank lower, but in no case at a higher rank." Once in the AUS, of course, the individual is eligible for promotion under established procedures, an opportunity for which he was not eligible as an ASC officer.

A $5,000,000 Program to Curb Industrial Accidents

Faced by mounting casualties on the home front already exceeding those on the fighting front, a program to curb the sabotage of manpower by accident is being organized by the War Production Fund to Conserve Manpower of National Safety Council. As reported previously (*EE, May '42, p. 333*), William A. Irvin, former president of the United States Steel Corporation, was named chairman of the Fund; Thomas W. Lamont of J. P. Morgan and Company is treasurer. A goal of $5,000,000 has been agreed on, this sum to be raised from business and industry throughout the United States.

A national committee of more than 600 members has been formed, with an executive committee of 74—both made up of senior executives in nationally prominent firms. A preliminary canvass through the national connections of major companies has yielded more than a million dollars in cash and nearly another in oral commitments. At present regional campaigns are getting under way in major industrial centers from coast to coast.

Many big firms have contributed heavily to the War Production Fund despite the fact that they already have excellent safety programs in effect. They are aware that some outside agency with specialized experience is needed to co-ordinate safety off the job, where three out of five accidents affecting workers happen, and to furnish technical assistance in handling the brand new problem of safety for the army of women in industry and of safety in the homes temporarily neglected for drill press and assembly bench. The same goes for accident-prevention methods to protect teen-age boys and older men now filling the shoes of men drawn into the armed forces.

The National Safety Council has formulated a plan for putting the $5,000,000 to work. No radical departures from previous practice are contemplated. The general strategy will be to speed up the existing program, and to get more coverage, more technical assistance in the field, and above all, more public co-operation in accident prevention.

WPB Industry Branches Reorganized

Reorganization of industry branches of the War Production Board, giving them greater strength, is being undertaken by the office of program determination under the direction of Ferdinand Eberstadt, vice-chairman of WPB, and Ernest Kanzler, director general for operations. One of the major reasons for strengthening branches is to enable them to handle the additional burdens to be placed on them by the new Controlled Materials Plan (*this issue, pp. 630–1*).

To as great an extent as possible, each industry branch will follow a similar pattern. Many of the functions of the office of the director general for operations will be assigned to branches, making them responsible for all operating phases such as the execution of programs, policies, and procedures established for the resources assigned to branches.

Permanent connection with industry will be maintained through active industry advisory committees assigned to each branch. Claimant agencies—Army, Navy, Maritime Commission, Office of Civilian Supply, Lend-Lease, and so forth—will assign permanent representatives to each branch. Permanent connection with labor also will be maintained through representatives assigned to each branch.

Industry advisory committees assigned to the branch will endeavor to put upon industry a greater measure of responsibility for meeting war production problems. They will get advice from the industry on methods of increasing supply where shortages exist, of controlling the distribution resources and of eliminating nonessential uses of resources. Likewise, they will obtain data on available and anticipated supply of resources and requirements for civilian use and for maintenance, repair, and spare parts.

Representatives of governmental claimants in the industry branch will submit data on requirements for resources and advise and assist in apportioning the available supply in accordance with the determinations of the requirements, program adjustment, and facilities committees. Claimants, in addition will assist in the processing of various forms controlling the distribution of resources.

Canada Represented on Production and Resources Board

Canada has recently been admitted as a member of the Combined Productions and Resources Board, a council maintained jointly along with the United States and Great Britain in recognition of the interlocking war economies of the three nations. She is represented by the Honorable C. D. Howe, Canadian minister of munitions and supply, who takes his place on the board with Donald M. Nelson and Oliver Lyttelton.

A joint war production committee of the United States and Canada has been operating for almost a year, and has achieved pronounced success in increasing war output by arranging rapid exchanges of supplies to avoid production delays, reducing duplications, breaking transportation bottlenecks, eliminating tariff and other barriers, and revising specifications to increase the number of common-type weapons. Typical of the co-operative industrial relationship between the two countries is the record of 62 Liberty-type merchant ships built in Canada with steel plate made available by the United States.

According to estimates, one-quarter of Canadian production is now going to fill United States orders, while about one-third of Canada's war goods is being produced for Great Britain.

Wider Distribution of War Orders to Small Manufacturers Planned

Procurement officials from the United States Army, the United States Navy, and the War Production Board recently issued directives indicating a renewed effort to bring smaller war plants more effectively into the nation's war-production program. Salient points in the memorandums issued by Lieutenant General Brehon B. Somervell to the chiefs of the Services of Supply and by Under-Secretary of the Navy James V. Forrestal were reiterated as follows in the general statement made by Lou E. Holland, deputy chairman of the WPB on Smaller War Plants and chairman of board

of the Smaller War Plants Corporation:

1. The Smaller War Plants Division, basing its information on advance requirements of suitable items sent out by the War Department or the procurement agency, will inform these offices of sources of supply among the smaller plants with suitable production facilities to furnish necessary items by required delivery dates.

2. Maximum possible subcontracting will be used in the production of items which, because of their complexity, must be awarded to the large organizations.

3. The Smaller War Plants Division agrees to maintain a list of small plants adaptable for the production of specific items, and to co-operate with the procurement agencies in the placement of prime or subcontracts by proving the suitability and competence of the proposed small plant and certifying as to its credit.

4. The department or agency, in conjunction with the Smaller War Plants Division, will examine existing prime and subcontracts with a view to securing further subcontracting whenever possible, and the agencies will attempt to secure the agreement of the contractors to further subcontracting whenever practicable. Cancellation of orders for new machine tools and equipment will be made in all cases where the corresponding operations can be handled by subcontracting, even though it is necessary, as the result of such subcontracting, to amend the contract to provide for increased costs because of less efficient methods of production.

5. The Smaller War Plants Division will provide planning, engineering, and production assistance to small plants, so that they may execute in a proper and satisfactory manner any contracts undertaken.

Foreign Patent Specifications to Be Available

Drawings and specifications of foreign-owned patent applications seized by the Office of the Alien Property Custodian will be printed and made available to American industry at a nominal price, Leo T. Crowley, Alien Property Custodian, announced recently. These applications ordinarily cover latest developments in patentable fields and many can improve American processes and devices. This departure from the traditional secrecy with which patent applications heretofore have been cloaked, it was stated, is in accordance with instructions from President Roosevelt that the Office of Alien Property Custodian use all means at its disposal to put enemy-owned property and patents to work in support of the nation's war effort.

Publication of the printed copies of patent applications will begin during December. Applications will be listed as they are printed, in classified order, in the *Official Gazette* of the United States Patent office. Announcement of the cost of copies and of the method of purchase will be made in the same publication. Meanwhile, the alien property custodian will make the information contained in the files of these applications available, in so far as is practical, to any person residing in the United States having a legitimate interest therein.

Any registered patent attorney may obtain permission to inspect (and make copies of) the file of a vested application (other than an application which stands under secrecy order) upon filing with the patent prosecution section, Office of the Alien Property Custodian, Washington, D. C., a request upon a form, "petition to inspect," which will be furnished upon request by the Custodian's Washington, D. C., Chicago, Ill., or New York, N. Y., offices.

Air-Raid Warning Device

Air-raid warning device developed in the carrier current laboratory of the General Electric Company, Schenectady, N. Y., provides audible and visible warning signals. It can be plugged into a house circuit and is designed for use on 720-cycle carrier-current systems. Operated by a special control board attached to the carrier-current transmitter at the power station, it would relieve telephone lines for other purposes during a raid by using existing power lines

War Production Drive Speeding Output. Labor-management committees, with the co-operation and assistance of the War Production Drive Headquarters, Washington, D. C., are conducting campaigns to stimulate production in American factories, mines, collieries, railroads, mills, and war plants. Names of the 1,600 plants in the United States now having labor-management committees and the committee chairmen have been published in a pamphlet available from the War Production Drive Headquarters, Raleigh Hotel, Washington, D. C. Basic to the formation of these committees is the exchange of ideas between labor and management and the granting of awards for increased output (*EE, September '42, p. 492*). The WPD headquarters has received a number of unsolicited reports from industries which indicate that since those plants inaugurated the campaign their production has increased 35 per cent in some cases.

Technical Books Requested for War Prisoners

Requests for technical books for allied prisoners of war in enemy prison camps have recently been forwarded to AIEE headquarters by the War Prisoners' Aid of the Young Men's Christian Association. The "Men of Science—Prisoners of War" Service of the YMCA War Prisoners' Aid is attempting to supply the needs of professional men and advanced students who are facing perhaps many more months of monotony in prison camps. It is stated that arrangements with the censors make it possible to send books that were published before November 1941. Books on the following subjects are currently requested:

1. Electrical supply and installation.
2. Modern power station equipment.
3. Electric traction.
4. Electrical engineering classified examples.
5. 20 to 30 Greek books on science.

In order to avoid possible censorship difficulties, please send the titles of any books available to R. D. Jameson, technical adviser, Library of Congress Annex, Washington, D. C. He will send instructions about getting the books to the War Prisoners' Aid.

The War Prisoners' Aid has already furnished many scientific books and reports the gratitude of the prisoners who receive them as they are thereby enabled to keep up with their scientific or professional work.

Power Pool Operates in Northwest. A significant development in the history of the electric utility industry in the Pacific Northwest occurred recently—connection of all major electric systems in one power pool. The move substantially increases the area's capacity to handle new war loads. The new supersystem, in which participants linked both their hydro and steam plants, increases over-all capacity by capitalizing on the fact that peak loads come at varying times and seasons in the different communities. Interchange of power is possible even though there is no direct connection between certain cities because energy is delivered through the supercircuit to all cities connected to the loop.

INDUSTRY • • • • •

Position to Be Filled Through Civil Service Examinations

Notice of the following position, which will be filled through civil service examinations, is published here as a service to members of the Institute. Application forms and full information as to requirements for examinations may be obtained from the secretary of the Board of the United States Civil Service Examiners at any first- or second-class post office, or the United States Civil Service Commission, Washington, D. C.

Materials Inspector. $2,600 a year salary. No age limits. To qualify for the position, applicants should show appropriate experience in the inspection, or testing for compliance with specifications, of a wide variety of either electrical or mechanical equipment. Broad progressive mechanical or skilled production experience in the manufacture of mechanical or electrical equipment, or experience as journeyman machinist, or journeyman toolmaker is acceptable. Applicants should be able to make written reports clearly and intelligently. The United States Civil Service Commission will continue to accept applications until further notice. No written test is required. Applications are not desired from persons engaged in war work unless a change of position would result in the utilization of higher skills possessed by the worker.

Westinghouse New District Office. The Westinghouse Lamp Division, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., recently replaced its branch office organization in Philadelphia with a complete district office. This new Middle Atlantic District is managed by Harry A. Croasdale, formerly assistant manager of the Middle Western District and at one time branch manager in Philadelphia. The new district will include seven counties in New Jersey, the entire states of Delaware, Maryland, and Virginia, the District of Columbia, and parts of West Virginia and Pennsylvania.

IT and T Associate Companies Merge. The International Telephone and Radio Manufacturing Corporation, and the Federal Telegraph Company, associate manufacturing companies in the United States of International Telephone and Telegraph Corporation, New York, N. Y., have been merged to form the Federal Telephone and Radio Corporation, Newark, N. J. This organization is now devoted almost entirely to the production of communication and radio equipment for the war effort. Construction of the first unit of a new factory which will house the new corporation will start shortly.

OTHER SOCIETIES •

Electrochemical Society Forms New Unit. The corrosion division, a new unit of The Electrochemical Society, was formed October 9, 1942, at the society's fall convention in Detroit, Ill. Growing out of the interest of a large number of the members in corrosion, the division is aimed at co-ordinating these interests with organizations and individuals already active and interested in this field, and will provide a channel for papers and symposia. Officers of the division for 1942–43 elected at the meeting are: L. G. Bogart, research and development laboratories, Crane Company, Chicago, Ill., *chairman;* H. H. Uhlig, research laboratory, General Electric Company, Schenectady, N. Y., *vice-chairman;* and R. H. Brown, metallurgical division, aluminum research laboratories, Aluminum Company of America, New Kensington, Pa., *secretary.* Nonmembers who are especially interested in corrosion work are invited to join The Electrochemical Society and to participate in the work of the corrosion division.

ASA Issues Revised Standard

A newly revised standard "Illuminating Engineering Nomenclature and Photometric Standards" (Z7.1–1942) developed under the leadership of the Illuminating Engineering Society, has been recently published by the American Standards Association. This standard provides the practical language of illuminating engineering and photometry, and in addition, contains tables on relative luminosity factors; wave-length units; relative erythemal factors; distribution coefficients for equal-energy stimulus; relative magnitudes of units of illumination and relative magnitudes of units of brightness; a listing of units, symbols, and abbreviations; and equivalents and conversion factors. Most of the fundamental definitions included in this standard have been adopted by the International Commission on Illumination, and are included in the multilingual vocabularies compiled by that commission and by the International Electrotechnical Commission.

Copies of this standard are available at 25 cents per copy from the ASA, 29 West 39th Street, New York, N. Y.

Future Meetings of Other Societies

American Association for the Advancement of Science. Annual meeting, December 30, 1942, New York, N. Y.

American Institute of Mining and Metallurgical Engineers. Annual meeting, February 14–18, 1943, New York, N. Y.

American Physical Society. 252d meeting (annual) December 28–30, 1942, New York, N. Y.; 253d meeting, December 29, 1942, Los Angeles, Calif.

American Society of Civil Engineers. Annual meeting, January 20–22, 1943, New York, N. Y.

American Standards Association. Annual meeting, December 11, 1942, New York, N. Y.

Society for the Promotion of Engineering Education. Middle Atlantic states section winter meeting, December 5, 1942, New York, N. Y.

Technical Valuation Society. Annual forum, December 12, 1942, New York, N. Y.

AAAS Engineering Section to Meet

Section M (engineering) of the American Association for the Advancement of Science will hold its sessions in connection with the 1942 annual meeting of AAAS on Wednesday, December 30, at the Hotel Commodore, New York, N. Y.

The 1942 program of Section M will feature two important subjects in fields where engineering must lean heavily on science. At the morning session the subject will be aeromedicine.

The subject of the afternoon session will be the dehydration of foodstuffs.

The program of the meeting:

Wednesday, December 30

9:00 a.m. Morning Session

1. "The Tilting Ballistocardiograph: Apparatus for Recording the Thrust of the Heart," **Robert W. Wilkins**, M.D., department of clinical research and preventive medicine, Robert Dawson Evans Memorial, Boston, Mass.

2. "Development of Instruments for Test and Classification of Flight Personnel," **E. Lodwig**, chief engineer, and **J. Zaleski**, design engineer, Mobile Refrigeration, Inc., New York, N. Y.

3. "The Application of Engineering Principles to Clinical and Aviation Medicine," **Alvan L. Barach**, M.D., associate professor of clinical medicine, Columbia University, New York, N. Y.

12:00 m. Luncheon Session

"The Romance and Engineering of Food Preservation," **Willis R. Woolrich**, dean of engineering The University of Texas, Austin.

2:30 p.m. Afternoon Session

1. "Theory of Processes," **H. J. Masson**, professor of chemical engineering, College of Engineering, New York University, New York, N. Y.

2. "Application of Theory to Manufacture," **Graham L. Montgomery**, associate editor, *Food Industries*, New York, N. Y.

3. "Military and Civilian Practice," Captain **Robert P. Melson**, Subsistence Research Laboratories, United States Quartermaster Corps, Chicago, Ill.

ASA 1942 Price List Published

The American Standards Association has announced the publication of its newest list of American Standards for 1942. More than 550 American Standards are listed, of which 71 represent new and revised standards approved since the last issue of the list (February 1942). These are marked with an asterisk. There is a separate section for American War Standards, standards developed specifically for the war effort. Another section is devoted to American Safety Standards, which are also of great importance to the war effort in connection with the President's program for the conservation of man-power. Other standards include definitions of technical terms, specifications for metals and other materials, methods of test for the finished product, dimensions, and so forth. They reach into every important engineering field and serve as a basis for many municipal, state, and Federal regulations.

In each case, the standards approved by the ASA represent general agreement on the part of maker, seller, and user groups as to the best current industrial practice. More than 600 organizations are taking part in this work. This list of American Standards for 1942 will be sent free of charge to anyone writing in for it. Requests should be addressed to the American Standards Association, 29 West 39th Street, New York, N. Y.

American Section of CIE Revived

The American section of the Chinese Institute of Engineers, first organized in the United States in 1919 under the name of Chinese Engineering Society, has been reorganized. At a meeting held recently the following officers were elected:

President: L. F. Chen; *vice-presidents:* A. T. Liu, P. H. Chin; *executive officers:* T. C. Hsiung, C. H. Tang, C. H. Wang.

Following its inception in the United States the society was reorganized in China under its present name and maintained an American section. After the outbreak of the war with Japan, many members in America were called to duty in China and consequently during the last few years the American section has been inactive. Realizing the importance of reviving its activities, the Chinese engineers in the United States are forming local CIE chapters in New York, N. Y., Washington, D. C., Detroit, Mich., Ithaca, N. Y., Boston, Mass., and on the west coast. The total membership of the American section is expected to be between 300 and 400 soon and is divided into the following classifications:

Mechanical engineering, electrical engineering, civil engineering, chemical engineering, mining and metallurgical engineering, and aeronautical engineering.

Besides arranging for local engineering lectures and discussions and also publishing a bimonthly CIE News Bulletin and a CIE Engineering Journal, contacts have recently been established with several leading U. S. engineering societies and institutes with a view to exchange of publications, to arrange for or participate in special lectures and, in general, to assist and give strength to this newly revived organization of Chinese engineers, the only one of its kind in the United States. In co-operation with this CIE policy, AIEE publications are already being exchanged with the CIE American section and the AIEE New York section plans to invite CIE members to its meetings.

LETTERS TO THE EDITOR

INSTITUTE members and subscribers are invited to contribute to these columns expressions of opinion dealing with published articles, technical papers, or other subjects of general professional interest. While endeavoring to publish as many letters as possible, Electrical Engineering reserves the right to publish them in whole or in part or to reject them entirely. Statements in letters are expressly understood to be made by the writers. Publication here in no wise constitutes endorsement or recognition by the AIEE. All letters submitted for publication should be typewritten, double-spaced, not carbon copies. Any illustrations should be submitted in duplicate, one copy an inked drawing without letter, the other lettered. Captions should be supplied for all illustrations.

U. S. Patent System

To the Editor:

Lately there has arisen a new crop of crusaders who consciously or unconsciously are attempting to wreck the American patent system, which in the past has contributed so largely to our material progress. The president of the United States has wisely appointed a patent-planning commission of eminent men, charged with the duty of making a survey of the entire patent situation and making recommendations for the improvement of our patent system. But the crusaders, not willing to wait for the report, have introduced in congress a flock of bills, most of which would emasculate completely the patent system and wipe out any benefits to be derived therefrom by the public.

Engineers have a vital interest in the patent system and yet the majority are woefully ignorant of the contents of the proposed bills. Many have never troubled to apply engineering thinking to the garbled statements and to reports regarding the functioning of the patent system which appeared in the newspapers. The articles given in the accompanying bibliography should be profitable reading to every engineer.

The Engineers' Society of Milwaukee and affiliated societies have felt that some positive action should be taken to arouse the interest of engineers, and a special meeting of all technical men was held on October 1, 1942, at which Mr. William F. Buckley, a Milwaukee patent attorney, spoke on the "Constitutional Right for Protection to Inventions", and Mr. W. E. Crawford (F '36), spoke on the "Future of Inventions in Relation to Science and Engineering in Itself."* As a result of the ensuing discussion, a resolution was passed asking congress to defer any patent legislation until the report of the patent-planning Commission was obtained.

It is the writer's belief that it would be profitable for other engineering groups to hold similar meetings. The national engineering societies could well afford to arrange for discussion of the subject at one of their national meetings. The engineer, as a professional man, owes it to the public to be informed in such matters and to help in guiding the attention of the public towards an unbiased appraisal of the role which patents and inventions play in the social and economic structure of the United States.

* The essential substance of this address is scheduled for early publication in *Electrical Engineering.*

REFERENCES

1. The Relation Between Patent Practices and the Antimonopoly Laws—Part I, **G. S. Rich.** Patent Office Society *Journal*, volume 24, February 1942, pages 85–106; Part II, March 1942, pages 159–81; Part III, April 1942, pages 241–83; Part IV, May 1942, pages 328–56; Part V, June 1942, pages 422–37.

2. Inventions, Patents, and Victory, **J. D. Cunningham.** Patent Office Society *Journal*, volume 24, July 1942, pages 451–7.

3. Patent Laws in Our Social Scheme, **Nelson Littell.** Patent Office Society *Journal*, volume 24, August 1942, pages 520–30.

4. We Depend on Invention, **Lawrence Langner.** Patent Office Society *Journal*, volume 24, August 1942, pages 545–64.

5. Chemists Should Read Patents, **R. Frankl,** Patent Office Society *Journal*, volume 24, August 1942, pages 565–74.

6. An Opportunity for the National Patents Planning Commission, **Norman Holland.** Patent Office Society *Journal*, volume 24, September 1942, pages 592–8.

7. The American Patent System, **Wallace H. White, Jr.** Patent Office Society *Journal*, volume 24, September 1942, pages 599–628.

ARTHUR SIMON (F '13)

(Patent attorney and engineering consultant, Milwaukee, Wis.)

The Effect of Initial Conditions on Subharmonic Currents

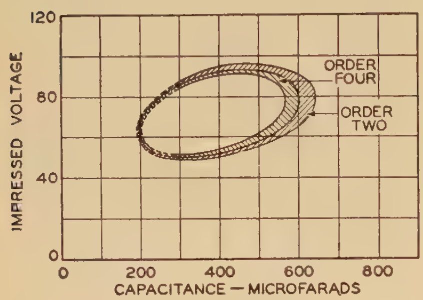
To the Editor:

The paper on "The Effect of Initial Conditions on Subharmonic Currents in a Nonlinear Series Circuit," by Stephen J. Angello in the AIEE *Transactions*, volume 61, 1942, September section, pages 625–7 was particularly interesting. On the basis of a series of experiments, we should like to add to this very instructive paper the following information on the stability of such oscillations.

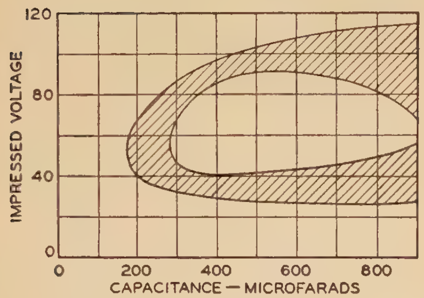
In a test circuit like the one used by Mr. Angello, which is composed of a capacitor, an iron-cored inductor and a small resistor, it is found that for certain combinations of capacitance and rms values of impressed voltage, sustained subharmonic oscillations of several orders can be obtained. The order of subharmonic oscillations is defined by the number of complete cycles of impressed voltage during which the current wave completes one cycle. For a fixed inductor and a constant capacitance, there will be a range of voltages for which sustained subharmonics of a particular order can be initiated. This range is usually rather sharply defined. The shaded area of Figure 1 shows a region, within which subharmonic oscillations could be initiated and sustained. It will be noted that the region defining subharmonic oscillations of the fourth order is surrounded by a region of oscillations of the second order.

The most common subharmonic oscillation is that of order three, and since the region for this order is rather large especially for low values of resistance in the circuit normally little trouble will be encountered in sustaining oscillations of this order.

There is usually some difficulty involved in the initiation of subharmonic oscillations of the second order, or of oscillations of higher order than three. The voltage range which permits the initiation of sustained oscillations of one order is rather narrow for the capacitor used in our experiment—in the neighborhood of four or five volts, except for a range of capacitances from about 550 to about 650 microfarads. This can be seen readily from the graph in Figure 1, where capacitance is plotted versus rms values of impressed voltage.



(*a*)



(*b*)

Figure 1. Regions of subharmonic response; resistance, 1.27 ohms; inductor, 110-volt winding of a $7^1/_2$ kva, 110–220-volt transformer

(*a*) *Orders two and four* (*b*) *Order three*

Most or all of the following conditions must be fulfilled, if the oscillations are to be sustained:

(*a*) The capacitor must be initially charged to a minimum value.

(*b*) The initial flux linkages due to residual magnetism must have a definite value.

(*c*) The circuit must be closed at a certain point on the voltage wave.

(*d*) The resistance of the test circuit must not be too high.

It appears reasonable that oscillations of this kind are difficult to sustain even if the above conditions are fulfilled, since the peak values of currents of higher order subharmonics are often extremely high, and poor regulation of the voltage supply causes immediate changes of impressed voltage which in all likelihood will stop the oscillations.

Therefore it seems permissible to conclude that these oscillations of the second order and of orders higher than three might be assumed unstable, since they might persist for only a short duration and are apt to disappear because of the slightest disturbance of the circuit. The abnormally high currents associated with such subharmonic frequencies favor this "unstability" due to line regulation.

CHARLES F. SPITZER (Enrolled Student)

THOMAS P. EVANS (Enrolled Student)

(Dunham Laboratory, Yale University, New Haven, Conn.)

Temperature and Electric Stress in Impregnated-Paper Insulation

To the Editor:

In the interesting paper "Temperature and Electric Stress in Impregnated-Paper Insulation" by J. B. Whitehead and W. H. MacWilliams, published in AIEE *Transactions*, volume 61, January section, pages 10–13, which owing to war conditions reached me only recently, power factor-stress curves of the characteristic shape given by liquids containing ions the motion of which is limited by solid barriers are obtained.

The stress corresponding to maximum power factor in these curves may be used, in conjunction with other data, to calculate the concentration, mobility, and effective radius of the ions concerned—information which cannot so readily be obtained by any other method. It is therefore important that the factors controlling the shape of the curves should be correctly explained.

Possibly owing to a need for brevity, the authors have given an explanation which is correct only in regard to the falling portion of the curve. Regarding the rising portion, they write ". . . increasing stress in the low range is accompanied by increase in the lengths of the excursions of the free ions . . . and consequently increases in conductivity and loss."

It is true that an increase in the ionic path proportional to increase of stress will result in an increase of total loss, but the same is not true of the conductivity, as distinct from the current conducted. A liquid in which ionic path is proportional to stress will possess a constant conductivity and therefore a constant loss angle up to that stress at which further motion of the ions is impeded by some barrier. The cause of the observed initial increase in loss angle must be sought elsewhere.

In a quantitative analysis of curves of this type, I have given an explanation of the rising portion of the curves which appears to check adequately with experimental data.[1] This is based upon the fact that the applied stress must, during the greater part of each cycle, segregate the positive from the negative ions, and thus reduce their effective rate of recombination. The rising part of the curve is thus due to a temporary increase in the number of ions so long as the field is applied, and dependent upon the magnitude of the field. The numerical value of the increase may easily be calculated, and agrees with experimental results at stresses which are not too low. At extremely low stresses, a further correction must be applied due to the image forces upon the ions, from the proximity of the walls of the pores in which they exist.

It is of historical interest that curves of this shape were reported so long ago as 1928 and it is remarkable that their value as a means of research into the characteristics of ionisation in liquid dielectrics should have escaped notice until the present time.[2]

REFERENCES

1. Dielectric Loss in Thin Films of Insulating Liquids, **C. G. Garton.** Institution of Electrical Engineers *Journal*, volume 88, April 1941, pages 103–20.

2. Dielectric Losses in Oil-Impregnated Paper, **E. R. Le Ghait.** *Electric Journal*, volume 25, April 1928, pages 187–93.

C. G. GARTON

(British Electrical and Allied Industries Research Association, London, England)

Detecting the Saboteur in Gasoline Fires

To the Editor:

The article "Detecting the Saboteur—Static Electricity—in Gasoline Fires" by Robin Beach (*EE, October '42, pp. 501–08*) recalled to the writer an actual happening that might be of interest to readers.

Two 1,500-barrel steel tanks with about 12-inch manholes in the center of the top were set near a railroad siding. These tanks were set on a limestone hill (typical in Kansas) with the upper soil removed to provide a flat place on which to set them. Crude oil was pumped into these tanks so that it could be loaded into tank cars for shipping. This crude oil was fairly high gravity with lots of dissolved gasses, and was pumped through a 4-inch pipe line from the oil field some ten miles away.

The writer, being a young engineer, was in charge of engines and pumping equipment and had nothing to do with pipe lines and tanks. The line was run in the usual oil-field manner covered up in the ground for about ten miles. The end of the line ran up over the tanks and was supported by placing wood, four inches by four inches, on top of the tank for support (Figure 1). The necessary ells, valves, and so forth allowed the line to discharge into the tanks through the manholes in the tops and were centered in the manholes as nearly as possible, all of which kept the well-grounded pipe line well insulated from the ungrounded tank. This made excellently designed spark plugs, since the pipe line was everywhere (accidentally of course) well insulated from the tanks and was well

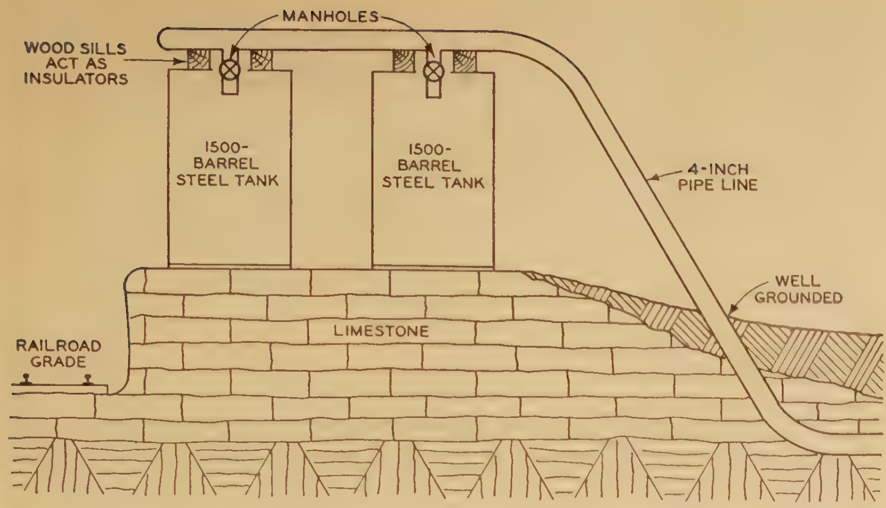


Figure 1

grounded itself. The manholes were always giving off highly inflammable gasses right in the manhole opening where the spark would occur, and with the tanks partly full of highly inflammable oil, conditions could not have been better for a static discharge between the discharge pipe and the edge of the manhole, whenever a highly charged cloud passed over just before a rain. After or during a rain the tanks would be more or less grounded by the limestone becoming wet.

The one in charge of construction and pipe-line officials for the company were told that a fire would be sure to burn the tanks up and that three feet of bonding wire and 30 minutes of work would probably save them. The tanks burned. It was too simple to bond them.

A. D. STODDARD (M '36)

(Vice-president, Haliburton Oil Well Cementing Company, Duncan, Okla.)

Electrical Heating of Overhead Conductors

To the Editor:

With regard to the electrical heating of overhead conductors described in the paper, "Sleet Problems on Electrified Railroads," by H. F. Brown (AIEE *Transactions, August section, pp. 589–93*), there is quoted from Pender's Handbook this formula:

$$I = K\sqrt{\frac{Td^3}{r}}$$

where

T is the temperature rise in degrees centigrade

d is the diameter of the conductor in inches

r is the resistance of the conductor in ohms per circular mil-foot at final temperature

K is a constant depending upon surface condition of wire; whose value is (approximately) 800 in still air and 1,000 in open air for solid wire; and 1,200 in open air for stranded conductors.

This shows that the heating current is directly proportional to the square root of the temperature rise, to the square root of the cube of the conductor diameter, and inversely to the square root of the resistivity of the conducting material at the final temperature.

The writer has constructed a nomogram for the graphical solution of this equation merely by drawing three lines across the scales (Figure 1). These computing secants are numbered one, two, three, and are drawn in that order, each one cutting the three scales marked thereon at the appropriate values. The use of the chart is therefore self-evident.

The computation drawn on the chart shows, for instance, that a solid copper conductor 1.31 inches diameter (d) at a final temperature of 100 degrees centigrade, being a rise of 80 degrees (T), will be carrying 3,620 amperes (I) in open air. The resistance of the mil-foot of copper wire at 100 degrees, as taken from the separate graph at upper right, is 13.7 ohms (r). Thus "one" is drawn through $r = 13.7$ and $T = 80$, and produced to pivot scale P. "Two" is drawn thence to 1.31 on d, through pivot scale Q; and "three," thence backwards to 1,000 on K, through I, which it cuts at 3,620 amperes, the required value.

For compactness the chart is conceived as folded over on the d scale so that the r scale and the K scale, which has the only three above mentioned points thereon, coincide.

CARL P. NACHOD (A '07)

Vice-president, Nachod and United States Signal Company, Inc., Louisville, Ky.)

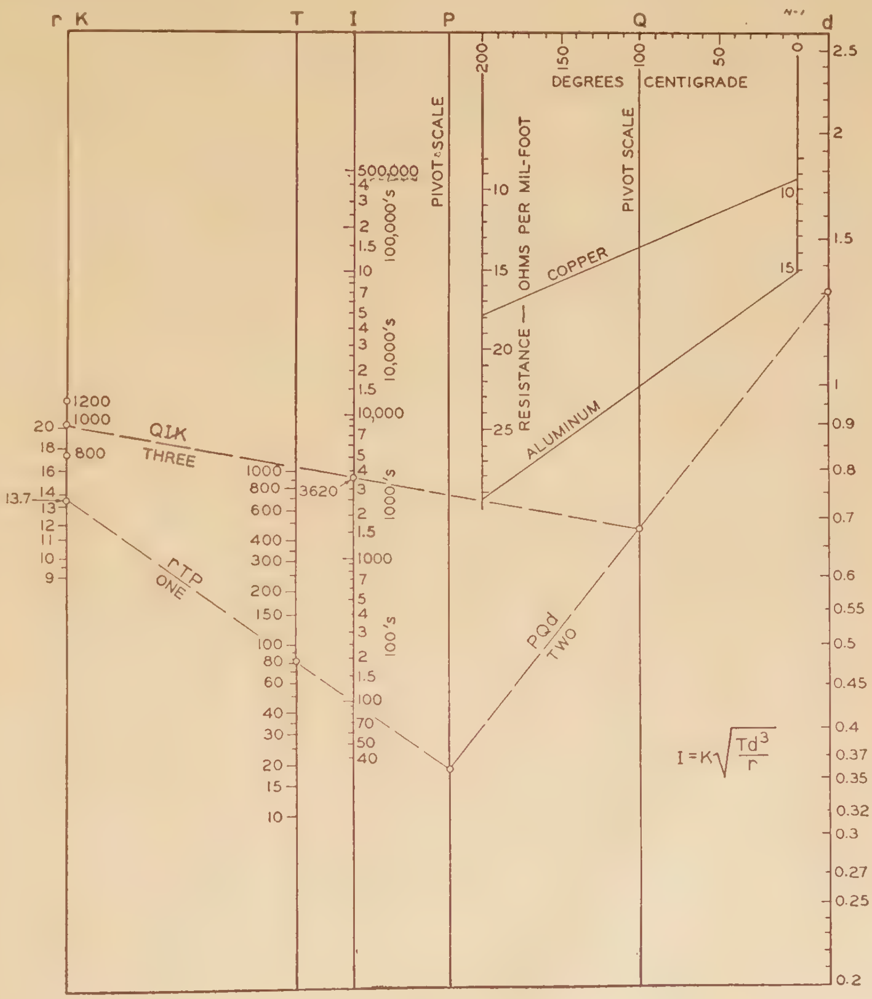


Figure 1

NEW BOOKS • • •

The following new books are among those recently received from the publishers. Books designated ESL are available at the Engineering Societies Library; these and thousands of other technical books may be borrowed from the library by mail by AIEE members. The Institute assumes no responsibility for statements made in the following summaries, information for which is taken from the prefaces of the books. All inquiries relating to the purchase of any book reviewed in these columns should be addressed to the publisher of the book in question.

Handbook of War Production. By E. A. Boyan. McGraw-Hill Book Company, Inc., New York, N. Y., London, England, 1942. 368 pages, diagrams, etc., $9^1/_2$ by 6 inches, cloth, $3. (ESL).

Discusses the problems of management that are involved in the conversion of plants to the production of war materials, and in increasing the quality and speed of output. It is based upon the experience of pioneer war manufacturers. Among the subjects discussed are the procurement of contracts, materials and supplies, production planning and control, quality control, labor and expansion, conservation of strategic material, industrial accounting in wartime, estimating for war contracts, and subcontracting.

Electrical Engineering (volume 2). By W. T. Maccall. University Tutorial Press, Ltd., London, England, 1942. 463 pages, illustrations, etc., 9 by $5^1/_2$ inches, cloth, 15s. (ESL).

This book is based on the author's "Alternating Current Electrical Engineering." With volume one of the present work it is intended to cover all the fundamentals of all branches of electrical engineering dealt with in the usual three-year courses given in British schools. This volume discusses symbolic notation, harmonic analysis, a-c generators and motors, converters, mercury-arc rectifiers, transmission, protection, and symmetrical components.

Microwave Transmission (International Series in Physics). By J. C. Slater. McGraw-Hill Book Company, Inc., New York, N. Y., London, England, 1942. 309 pages, $9^1/_2$ by 6 inches, cloth, $3.50. (ESL).

This book on ultrahigh-frequency systems presents the general theory underlying the methods used for transmitting microwaves from generator to receiver, including the intermediate stage of radiation from one antenna and absorption by another. The treatment is based upon the theory of conventional transmission lines and on Maxwell's equations. The book is an advanced text and reference book.

Handbook of Applied Hydraulics. By C. V. Davis. McGraw-Hill Book Company, New York, N. Y., London, England, 1942. 1,084 pages, illustrations, 9 by 6 inches, $7.50. (ESL).

A general reference work on hydraulics, composed of brief, yet complete texts on its various branches with practical information on the planning and design of hydraulic works. Hydrology, river regulation, dams, spillways, canals, hydroelectric plants hydraulic machinery, water supplies, sewerage, irrigation, and drainage are discussed by eighteen prominent engineers with experience in various fields.

Natural Trigonometric Functions (to Seven Decimal Places for Every Ten Seconds of Arc). By H. C. Ives. 2d edition, John Wiley and Sons, New York, N. Y., Chapman and Hall, London, England, 1942. 351 pages, diagrams, etc., 10 by 7 inches, cloth, $9. (ESL).

These tables give the natural sines, cosines, tangents, and cotangents. They also include eleven other tables frequently needed by surveyors. Errors in the previous edition have been corrected, and a table giving the tangents and cotangents to single seconds from 0 to 2 degrees have been added.

Basic Radio, the Essentials of Electron Tubes and Their Circuits. By J. B. Hoag. D. Van Nostrand Company, New York, N. Y., 1942. 379 pages, illustrations, etc., 9 by $5^1/_2$ inches, $3.25. (ESL).

The aim in this text is to select the radio tubes and circuits which experience has proved useful, to present a simple explanation of how they work and where they are applied, and to provide sufficient numerical constants and other details to make them readily understandable. It is intended for students with only a limited knowledge of physics and mathematics.

Torque Converters or Transmissions. By P. M. Heldt. P. M. Heldt, Nyack, New York, N. Y., 1942. 406 pages, illustrations, etc., $8^1/_2$ by 5 inches, cloth, $4. (ESL).

The past ten or fifteen years have seen many developments in transmissions for use with internal combustion engines in vehicles. This book brings together in convenient form for use by designers and students the wide variety of mechanisms which have been devised. Much information on design is provided, especially for the types in common use.

Modern Trigonometry. By M. J. G. Hearley. Ronald Press Company, New York, 1942. 168 pages, illustrations, 8 by 5 inches, cloth, $1.75. (ESL).

An elementary textbook presenting the numerical side of trigonometry in a form adapted to students with little mathematical background. The problems are practical ones, and the applications of trigonometry in astronomy, navigation, and mechanics are discussed. The book is designed especially for students of aeronautics.

The Steam Locomotive. R. P. Johnson. Simmons-Boardman Publishing Corporation, New York, N. Y., 1942. 502 pages, illustrations, $9^1/_2$ by 6 inches, cloth, $3.50. (ESL).

Discusses certain fundamentals of locomotive theory and operation. In addition, the economics of the steam locomotive are considered and comparison with the Diesel-electric variety is made. The book contains much of interest to designers and those concerned with railroad motive power.

Arc-Welding Job Training Units. Dunwoody Series Welding-Training Jobs. American Technical Society, Chicago, Ill., 1942. 103 pages, illustrations, etc., 11 by $8^1/_2$ inches, paper, $1.25. (ESL).

Presents a course in welding with the electric arc adapted to the needs of industrial schools and apprentice training. The course consists of forty jobs, an information sheet being furnished for each. Question sheets are also provided for most jobs. The course has been thoroughly tested in schools and industries.

Postwar Planning in the United States. By G. B. Galloway. Twentieth Century Fund, New York, N. Y., 1942. 158 pages, tables, 9 by 6 inches, paper, 60 cents. (ESL).

This report summarizes the activities, personnel, and publications of the various agencies engaged in research upon the economic and social problems that will face us when the war ends. A considerable bibliography is appended.

PAMPHLETS • • •

The following recently issued pamphlets may be of interest to readers of "Electrical Engineering." All inquiries should be addressed to the issuers.

Patents and Invention. National Association of Manufacturers, New York, N. Y., 1942. 20 pages, no charge for single copies.

Post-War Agenda. National Resources Planning Board, Washington, D. C., 1942. Outline chart.

Standards for Foam Extinguishing Systems. National Board of Fire Underwriters, New York, N. Y., 1942. 31 pages, $1.50 per hundred, $10 per thousand copies.

Ohio Stream Drainage Areas and Flow Duration Tables. C. H. Wall, C. V. Youngquist. (The Engineering Experiment Station Bulletin 111, Ohio State University Studies, Engineering Series, Columbus, 1942.) 73 pages, 75 cents.

Voluntary Labor Arbitration Rules of Procedure. Revised edition. American Arbitration Association, New York, N. Y., 1942. 35 pages, no charge.

TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Any discussion of these papers will appear in the December 1942 Supplement to Electrical Engineering—Transactions Section

Thermal Co-ordination of Motors, Control, and Their Branch Circuits on Power Supplies of 600 Volts and Less

B. W. JONES
ASSOCIATE AIEE

THE protection of a motor against an overload or the protection of a motor and its branch circuit against a short circuit are basically the same problem. The entire circuit is heated at a rate which is a function of the amount of current flowing, and this current must be interrupted before the winding or any part of the circuit exceeds a prescribed temperature. The means used to protect against these two magnitudes of power may be the same, or it may be entirely different as will be brought out later. It is the purpose of this article to show what the heating characteristics are of the motors, the control, and the branch circuit wiring, and what the characteristics of the protective devices should be in order that the power will be disconnected from the main feeders under all conditions of excess current before excess temperatures are attained.

The points which this article will discuss and some of the conclusions reached are:

1. That the short-time heating characteristic of three-phase induction motors stators can be shown in a simple graphical method when we know the current density in the stator windings at rated current. These data should then be used as the basis for determining the tripping characteristic of overload and short-circuit protective devices.

2. If the cross section of the reduced section of a zinc fuse link is not greater than any part of the copper conductors nor greater than 45 per cent of any Nichrome conductor (the heaters of overload relays), then the fuse will give good short-circuit protection to the copper and to the Nichrome conductors.

3. Small horsepower starters, which are within the range of from 1 to 50 horsepower at 440 or 550 volts, or of 1 to 25 horsepower at 220 volts (classified as National Electrical Manufacturers' Association size 3 or smaller), require a short-circuit protective device that will interrupt the circuit during a heavy short circuit in less time than one-half cycle. Tests have shown that a suitable size nonrenewable cartridge-type fuse of good design is capable of doing this.

4. Because of this fast interrupting ability, this type of fuse is current-limiting (see Figures 6 and 7). Therefore, it gives two-way protection, in that it both limits the amount of current which reaches the starters, and it is exceptionally fast when the currents are high.

5. Because of the inductance in a d-c generator, its current will build up at a slower rate during a short circuit than will the current in an a-c circuit, and therefore a fuse will be more current-limiting on a d-c system when supplied from a d-c generator than on an a-c system. Unlike the a-c power, the peak voltage and the average voltage is the same on d-c systems. Because of these two factors it is easier for a fuse to interrupt a short circuit on a d-c system than on an a-c system of the same rated voltage since the entire interrupting period takes place in a fraction of a half cycle.

6. A NEMA size 3 starter (50 horsepower at 440 volts or 550 volts, three phase) or smaller can be fully protected against any major damage from short circuits on a system having available currents up to 100,000 amperes, provided a correct current and voltage nonrenewable cartridge-type fuse of good design is placed in each branch circuit line, and provided the fuses are positively held in their clips so as to prevent magnetic forces from throwing them out.

7. A circuit breaker provided with a fast instantaneous magnetic trip, as used on our tests, demonstrated its ability to protect NEMA size 4 and larger starters, when on a system having an available current equal to or less than the interrupting rating of the breaker.

8. The maximum size fuse that should be used is $3^1/_2$ times the current rating of the overload relay, or four times the current rating of the motor, and if this does not result into the same current, then the smaller of the two should be selected. See Figure 3.

Permissible Temperatures

Let us first decide what are excess temperatures. Much has been written on this subject,[1] but it is quite generally agreed that 90 to 100 degrees centigrade is a satisfactory temperature (approximately 50 degrees rise), where it is maintained for long intervals, but where the temperatures are transient and last for only a few seconds or minutes and occur infrequently, then this 50 degrees rise can be materially increased. For conditions like a stalled squirrel-cage induction motor which lasts for only a few seconds the 50 degrees rise can be doubled. A rise of 100 degrees is satisfactory for a stalled motor because it will seldom be called upon to withstand this condition and then for only a few seconds.

Current-Time Heating Characteristics

Let us now ascertain the heating characteristic of a three-phase squirrel-cage type of induction motor. When a representative type of induction motor is running at rated load, approximately one third of its total heat loss will be produced by the iron, and the remaining two thirds will be produced by the copper. But if the motor is stalled and taking five times rated current, then the iron loss will re-

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Paper 42-79, recommended by the AIEE committee on industrial power applications for presentation at the AIEE North Eastern District meeting, Schenectady, N. Y., April 29–May 1, 1942. Manuscript submitted February 16, 1942; made available for printing March 30, 1942.

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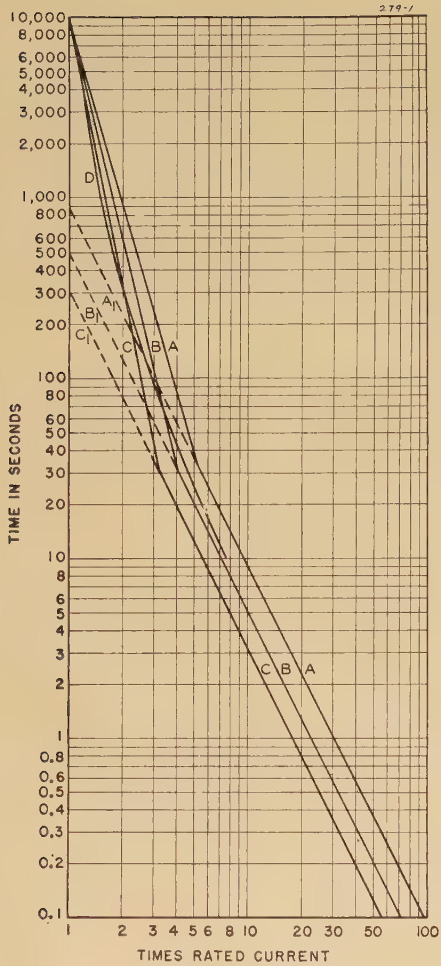
379-1 

Figure 1. Heating characteristics of three-phase induction motors. Also tripping characteristics of a representative temperature overload relay

Curve *A*—Current-time curve for copper, showing rate of heating where current density is 3,000 amperes per square inch at rated current, and maximum temperature reached is 100 degrees centigrade rise

Curve *B*—Same as *A* except current density is 4,000 amperes per square inch

Curve *C*—Same as *A* except current density is 5,000 amperes per square inch

Curves A_1, B_1, C_1—Extensions of *A*, *B*, and *C* to show the rate of rise if there were no heat loss

Curve *D*—Current-time tripping curve of a high-grade temperature overload relay

Because of heat conduction into the insulation and into the iron of the motor, and because of convection from the motor, the temperature rise for times of 30 seconds and longer will range between 50 degrees centigrade minimum and something under 100 degrees centigrade maximum

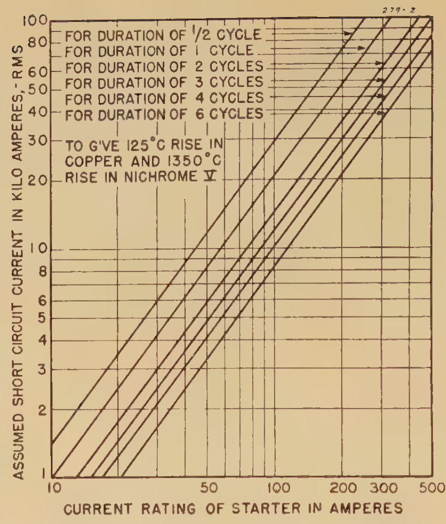


Figure 2. Required interrupting speeds fully to protect starters having heater-type overload relays against specified short-circuit currents

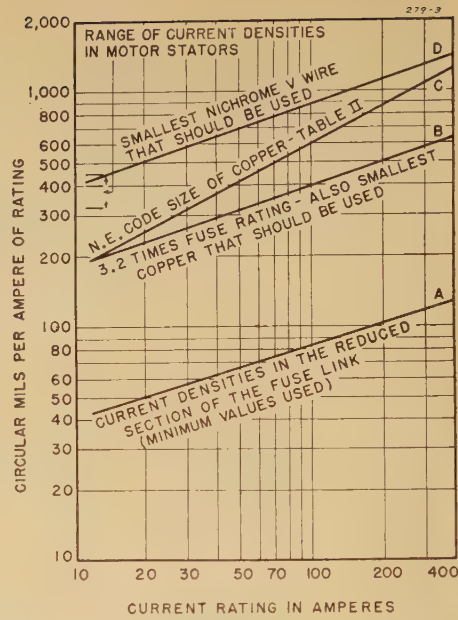


Figure 3. Current densities in copper and in Nichrome V wires, in stator windings of squirrel-cage three-phase induction motors, and in the restricted section of General Electric low-voltage cartridge-type fuses when carrying their respective rated currents

main the same, but the copper loss will be 25 times larger. Under either a running or a stalled condition, the copper will lead the iron in temperature, but in the stalled condition will be leading so far that we can assume the iron temperature will be stationary in temperature, while the copper is going its limit. The length of time required for the copper to go its limit of 100 degrees centigrade rise will depend upon the current density in the copper. Let us assume that the current densities in the stator copper of the standard lines of three-phase induction motors fall within 3,000 to 5,000 amperes per square inch when carrying their rated currents, then curves *A*, *B*, and *C* of Figure 1 show the time required for these three copper densities to reach 100 degrees rise for any current from 1 to 100 times their ratings. This is based on the assumption that all the heat remains in the copper, and none is conducted out into the insulation or iron, or carried off by convection or by radiation. For short circuits where the considered time is one second or less, this assumption is sufficiently correct, but for stalled conditions of the motor, or for heavy running overloads where the no-loss time required to reach 100 degrees rise is 30 seconds or more, there will be sufficient loss from the copper due to heat transfer that recognition should be made. Referring to Figure 1, it will be noted that at the 30-second position of the *A*, *B*, and *C* curves they have changed their current-time rate in order that this heat transfer can be approximately indicated in a simple diagrammatic manner. At the 30-second position, where the two curves diverge, we know that if no heat had escaped from the copper, it would have a 100 degrees rise, but we know that some heat has escaped during this 30 seconds, so the temperature will be below the 100 degrees rise (perhaps 85 or 90 degrees rise or less). Likewise, at the 10,000-second position, where the motor is carrying its rated load, the heat transfer has reached an equilibrium whereby the temperature rise on the copper is 50 degrees—the rated rise of the motor—therefore, along various positions of the *A*, *B*, and *C* curves between 30 and 10,000 seconds, the temperature rise gradually increases as the load increases, reaching something less than 100 degrees rise at a stalled condition.

Thus, by the use of log-log-paper a very simple graphical method can be used, wherein the well-known thermal capacity of copper and the assumed current density used in the motor design can be used as the basic factors in this graphical method. The resultant temperatures obtained in this simple manner will be sufficiently close for all practical purposes, and the temperatures will be on the conservative side. Therefore, the three straight lines in Figure 1 between 30 and 10,000 seconds, which are labeled *A*, *B*, and *C* can be used to closely represent the transient heating time-current characteristics of the stator of the three designs of motors which have the 3,000-, 4,000-, and 5,000-amperes per square inch densities in the copper when carrying their rated currents.

Required Temperature-Overload-Relay Characteristics

Thus, if these three time-current characteristic curves are used as the basis for designing three temperature overload relays such that the relay-tripping times never exceed those shown on these curves and yet keep fairly close to them, then these relays should be capable of protect-

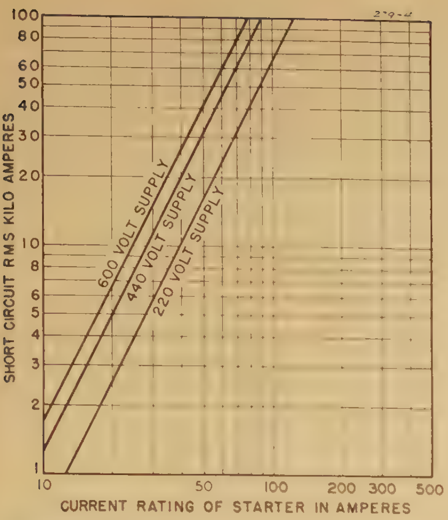


Figure 4. Magnitude of short-circuit currents for a circuit in which 20 volt-amperes per phase is assumed for the total impedance of the circuit when carrying rated current

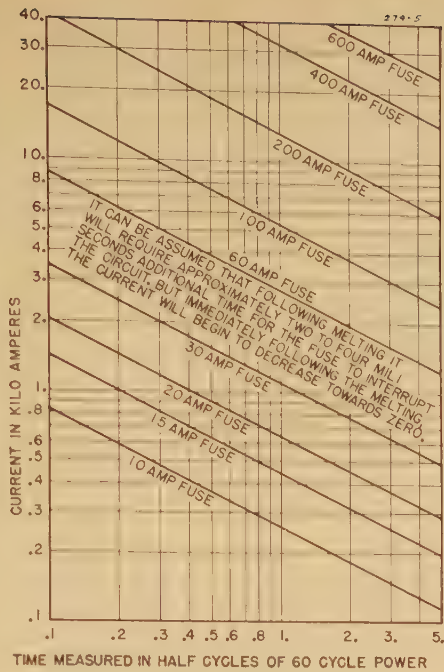


Figure 5. Time required for General Electric cartridge-type low-voltage fuse links (zinc) to melt

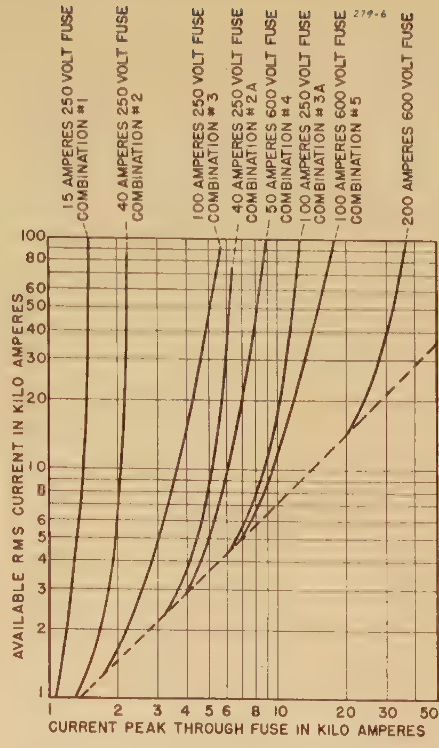


Figure 6. Current-limiting characteristics of one standard nonrenewable cartridge fuse per phase on a three-phase power supply

Combinations 1, 2, 2*A*, 3, and 3*A* were tested on 440 volts, three-phase power. Combinations 4 and 5 were tested on 600 volts, three-phase power

Starter Combination	Ohms Impedance
1	0.252
2	0.157
2*A*	0.029
3	0.065
3*A*	0.015
4	0.048
5	0.018

ing the three designs of motors against all magnitudes of overloads and yet permit them to do their full quota of work. However, the heater type of temperature overload relay, which develops its source of heat in one element—its heater—and transfers this heat to the bimetal or temperature sensitive element, tends to slow down on the high-current end such that the relay-tripping characteristic curve would normally cross the motor-heating curve at the high-current end. This type of relay must, therefore, be sufficiently fast at the stalled current value—approximately six times current—to give the required protection, and then at the lower current values of overload it will overprotect the motor. But since this is the characteristic of the relay most commonly built for the smaller motors, it follows that if this design is so made that it is correct for the stalled condition of the motor, it will be somewhat faster than necessary for all the lower degrees of overloads. This degree of overprotection is not generally sufficient to interfere with the motor's doing its required work.

The total thermal capacity of a motor is a function of its copper, its insulation, and its iron, but under a stalled condition the *effective* thermal capacity of this same motor is primarily a function of only its copper. Thus the effective thermal capacity of a motor under all degrees of overloads is a variable, having about a four-to-one range. Therefore, if a relay is designed which has a duplicate heating characteristic to that of the motor, then it must also have an effective thermal capacity which is a variable. This of course complicates the problem and results in a larger and a more complicated relay. Such a design is available and has been supplied since 1924, but it is not a large production unit because of its size and cost. Unless this desired characteristic can be designed into a relay which is both inexpensive and small, I doubt whether the trade will need this variable thermal-capacity characteristic sufficiently to demand it. Large motors will always require the best designs available, but the control for small motors will be determined largely by cost. Therefore, the small and inexpensive heater type of thermal overload relays will very likely be continued for some time.

The above discussion regarding how we can protect the insulation of a motor by protecting it against excess temperatures should not lead us to conclude that, if we do a good job in this respect, no insulation troubles will occur. These troubles may develop as a result of transient voltages, faulty work, mechanical injury, or from many other causes. Therefore means should be provided which will give adequate fault protection to the branch circuit, the control devices, the motor, and to the operating personnel.

Short-Circuit Protection

The devices which provide the degree of overload protection just discussed will not, in general, provide the necessary protection to the branch circuit and to the control against short circuits. In our discussion we have assumed that ten times rated current is the dividing line between overload and short-circuit conditions. Above ten times current the effective thermal capacity will be proportional to the copper in the stator winding of the motor and in the branch circuit and the control. Also we have assumed that 100 degrees centigrade rise will be the upper limit of temperature for the stator copper under overload conditions, but for short-circuit conditions 100 degrees rise is somewhat low. We will, therefore, assume that the temperature rise may be 125 degrees—a very, very conservative value—and that the Figure 2 curves will show the necessary speed of interruption required to keep the copper within these limits. The 125 degrees rise (165 or 175 degrees total) is not the limit to which it is permissible to carry copper with its insulation, but, when wire is selected for its continuous current-carrying ability, its resultant temperature under short-circuit condition will be in accordance with the above assumptions.

Since the path of the current which reaches the motor must travel by way of the conductor cable and the devices on the control panel, it will be necessary to also consider the heating characteristic of these current-carrying parts during this short-circuit condition. The conductor cables and contactor parts, we will assume, are made of copper and the heater of the temperature overload relays, which are in most motor circuits, is made of Nichrome. Therefore, we can assume that the path of the short-circuit current will be through copper and through Nichrome of known cross sections.

To protect the conductors adequately in the branch circuit, the control devices, and the motor winding, we must use a device that is both fast in opening the circuit and also has the necessary interrupting ability. The temperature overload relay, which is generally used to protect a motor against overloads, is far too slow to assist in protecting against short circuits. Also the line contactor, connecting and disconnecting the motor to and from the line, does not generally have the necessary interrupting capacity, even if the temperature overload relay could assist it in opening sufficiently fast—as it cannot do—and so we must look for other means which can function sufficiently fast and have adequate interrupting ability.

The most common form of short-circuit protective means used on low-voltage lines is cartridge-type nonrenewable fuses and air-break circuit breakers. If we consider first the fuse then a third material, namely zinc of the fuse link, will be added in the path of the short circuit. We must, therefore, consider the correct relationship between the cross sections of the zinc fuse link, the copper, and the Nichrome heater so that neither the copper nor the Nichrome will exceed certain prescribed temperature rises. To do this we will assume that when the fuse melts, the copper will have reached 70 degrees centigrade rise, the Nichrome 800 degrees rise, this fuse-melting period being one unit of time. Following the melting of the fuse, the current will be reduced to zero in an additional time which we will assume to be two units of time (this has been shown by tests to be a conservative assumption), and the rms current value, during this fuse-arcing period, we will assume to be 58 per cent of the value existing at the instant of melting. During the fuse-arcing period the copper is assumed to have an additional rise of 55 degrees and the Nichrome a rise of 550 degrees, making a total rise of 125 degrees for copper and 1,350 degrees for Nichrome.

Co-ordination of Fuse Size With Copper and Nichrome Wires

In setting up the basis for our problem as shown in Figure 3, we have selected the largest cross sections on the fuses and the smallest cross sections on the copper wire sizes, which means that there is some margin in our basic figures. Let us assume that a copper, a zinc, and a Nichrome wire are connected in series and that a given value of current is put through them. Also let us assume that the cross sections of the copper and of the zinc wires are the same, but the cross section of the Nichrome wire is larger by 2.2 times. The average resistance of

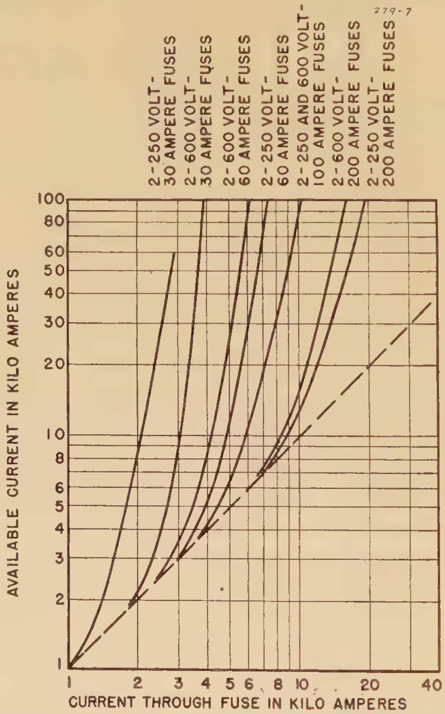


Figure 7. Current-limiting characteristics of standard nonrenewable cartridge fuse on 250-volt d-c power

zinc per circular mil foot between 20 and 419 degrees centigrade (melting) is 69 ohms, while the average resistance of copper per circular mil foot between 20 and 70 degrees is 12 ohms. This makes the average resistance of zinc 5.75 times larger than copper. Therefore, when the copper reaches 70 degrees rise the zinc should be $70 \times 5.75 = 400$ degrees centigrade rise assuming no loss of heat. Since the Nichrome wire, which is 2.2 times larger in cross section, is carrying the same current, and since its average resistance is 670 ohms per circular mil foot, the Nichrome wire should have a rise of $70 \times 670/12 \times (1/2.2)^2 = 800$ degrees centigrade.

Now, the above time is just sufficient to melt the fuse material; to raise the copper to 70 degrees centigrade rise; and the Nichrome to 800 degrees rise. At this time the current is a maximum, and a sufficient additional time must be allowed to reduce this current from this maximum value to zero. We will assume that this current reduces along a straight line so that the rms current is 58 per cent of the maximum.

Tests have shown that for very high currents, twice as much additional time should be allowed for interruption as was required to melt the zinc fuse. Then during this arcing period the rms value of current will be equal to 58 per cent of the value which melted it.

Therefore, if we initially allow a sufficient current to flow through the copper to cause it to reach a 70 degrees centigrade rise (from 20 degrees base) in a unit of time and then allow 58 per cent of this current to continue to flow for two units of time, the copper will have a 125 degrees total rise, because if a given current value flowing for one unit of time results in a 70 degrees rise, then 58 per cent of this current for two units of time will give $70 \times (0.58)^2 \times 2 \times 1.17 = 55$ degrees centigrade rise. This rise of $55 + 70 = 125$ degrees centigrade. (The 1.17 represents the average increase of the copper resistance due to the higher temperature.)

And if this same 58 per cent value also went through the 220 per cent cross section of Nichrome, it would have an additional rise over its 800 degrees centigrade value of $(125-70)680/14 \times (1/2.2)^2 =$ 550 degrees centigrade or a total of $800 + 550 = 1{,}350$ degrees centigrade. (The 680/14 also represents the increased resistance due to the higher temperature.)

Therefore, from this line of reasoning, which has been checked by test, we can expect satisfactory results if we have a given cross section of the zinc fuse link—the reduced part of the fuse link—and then see that no part of the copper cross section is smaller than this fuse cross section, and also see that the Nichrome cross section is not smaller than 2.2 times the fuse cross section.

To obtain the above cross-sectional relationships, let us call the current rating of the motor as 100 per cent; then the current rating of the overload relay will be 115 per cent; the current rating of the branch circuit wiring as given in the 1940 National Electrical Code, Table II, chapter 10 will be 125 per cent; and the current rating of the fuse will be 400 per cent. Figure 3 shows graphically the current densities in these three materials. For example a 25-ampere control should have copper cross sections of $250 \times 25 =$ 6,250 circular mils and Nichrome cross

sections of 550×25=13,750 circular mils as minimum cross sections while the maximum size fuse that should be used is 25×3.2=80 amperes, which should have the same cross section as the copper—6,250 circular mils. If the motor has a service factor of 1.15, then the motor rating would be 25 divided by 1.15=21.7 amperes.

Estimated Magnitude of Short-Circuit Current

It is frequently desirable to estimate the magnitude of short-circuit current that will pass through a given size starter, and then determine how long this current can be permitted to flow without exceeding the temperatures prescribed above. Figure 4 was developed on the basis of 20 volt-amperes per phase for the total impedance of the circuit when carrying rated current (this is an approximate minimum impedance generally found). This will hold fairly well for the smaller sizes of control (25 horsepower and less) where the impedance of the supply circuit is a small part, but for larger sizes of starters this method will show too large a value of current. But if an expected value of current is determined by this or other means, then Figure 2 will show how long this value of current can be permitted to flow and still keep the temperatures within the bounds set above, namely 125 degrees centigrade rise on the copper and 1,350 degrees rise on the Nichrome. For example, Figure 4 shows that for a 25-ampere starter at 600 volts the expected short-circuit current would be 10,000 amperes, while Figure 2 shows that a 25-ampere starter can withstand only 4,700 amperes for one one-half cycle or 3,300 amperes for one cycle of time duration. Therefore, since the heating varies as the square of the current this size of starter could carry 10,000 amperes for only $(0.47)^2$ (half cycle) or 0.00184 second. This is obviously faster speed than that of a circuit-breaker operation—which is in the order of 0.008 to 0.015 second—but as shown in Figure 3, a correctly selected fuse can do the job for any current because its time of operation will vary with the magnitude for the current.

Interrupting Speed of Fuses

Another pair of questions that are frequently asked is how fast will a fuse interrupt the circuit, and how much available energy can it interrupt? The first question must be divided into two parts in order that we might segregate the important factors, namely the length of time required to melt the link, and then the time required to interrupt the circuit. The time required to melt the link of General Electric nonrenewable fuses is shown in Figure 5, which gives the time in half cycles (60 cycles assumed). For a fuse to be current-limiting, it must melt in the early stages of a half cycle—such as one fourth of the half cycle—Figure 5 shows that a 100-ampere fuse of this type will melt in this time when approximately 10,000 amperes are put through, and Figure 6, which is a test record, shows that the available current may be around 10,000 amperes value. This means that the fuse stops the current from materially exceeding the value reached at melting. And of course, this effect becomes more pronounced on the smaller size fuses.

It is, therefore, obvious from Figures 5 and 6 that the degree of current-limiting becomes less and less as the size of fuses increases, and that for ordinary powered circuits the fuses larger than 200 amperes size do not give much current-limiting effects. It is also obvious that the 200-ampere fuse and smaller interrupt in one half cycle or less when subjected to currents of 10,000 to 20,000 amperes. This means that fuses do an exceptionally fast interrupting job in protecting motors and controllers of 100 horsepower (size 4 starters) and less.

Interrupting Ability of Fuses

The second half of the question dealing with the amount of available energy which these fuses can interrupt was determined by providing a 250-volt d-c source which was capable of delivering 96,000 amperes, and also a three-phase source which was capable of delivering an rms symmetrical current of 84,000 amperes at 440 volts and 114,000 amperes at 600 volts. In the d-c circuit was placed two standard nonrenewable General Electric 250-volt fuses in series to simulate the usual two fuses that are in a circuit. Two 600-volt fuses were also tested in this circuit, wherein the available currents were varied from 10,000 to 96,000 amperes in several steps, and the results were perfect. The build-up of the current in this circuit was much slower than in an a-c circuit so that the current-limiting effects were much more pronounced as shown in Figure 7.

In the three-phase 440-volt a-c circuit one 250-volt fuse of the above type was connected in each supply line, and the available currents ranged from 18,000 to 84,000 symmetrical rms amperes. The resulting currents are shown in Figure 6, and the performance was perfect. When the supply power was 600 volts, then one 600-volt fuse was connected in each supply line, and the available current ranged from 24,000 to 114,000 amperes. The results, which were perfect in operation, are also shown in Figure 6. It can therefore be said that with this arrangement of connections, these fuses are capable of interrupting a circuit, either a-c or d-c, which has an available short-circuit current of 100,000 amperes.

Conclusions

The following recommendations are made:

1. Correctly designed overload relays should be used to protect all sizes of motors against overloads. To do this, relays having three degrees of tripping speeds should be available—fast, medium, and slow tripping.

2. Every motor should be listed in one of three groups to show whether it is a fast-, medium-, or slow-heating unit. The correct overload relay could then be selected for each motor.

3. Full short-circuit protection for NEMA size 3 and smaller starters can be obtained by the use of correct-size nonrenewable cartridge-type fuses of a good design. Interrupting devices which are slower than these fuses may not give full protection.

4. NEMA size 4 and larger starters will be protected by fast-tripping air circuit breakers which have an instantaneous type of tripping device.

. The copper cross section of the branch circuit, the control, and the motor should be equal to or larger than the reduced cross section of the zinc fuse link, and the cross section of the Nichrome of the overload relays should be 2.2 times larger than the zinc fuse link.

6. Since safety first laws are becoming more stringent, since available short-circuit energy is rapidly increasing, and since starters are frequently mounted near workmen, it is quite essential that adequate short-circuit protection be provided for all branch-line circuits. Since it has been demonstrated that this protection can be provided for standard existing starters by means of standard fuses, even on large power systems, it was considered of sufficient importance to bring it to your attention at this time so that this needed protection can be obtained and especially during this critical war period.

Reference

1. AIEE Standards Pamphlet No. 1 A, September 1941. (Proposed supplement to AIEE Standards Pamphlet No. 1.)

Selenium Rectifiers and Their Design

JOHN E. YARMACK
ASSOCIATE AIEE

Synopsis: Selenium rectifiers are dry-plate rectifiers of the electronic type. The history of dry rectifiers began some 30 years ago with the advent of the copper-sulphide rectifier. In the early 20's, the copper-oxide type came into common use, and during the past three years, selenium rectifiers have found wide application in American industry. Fundamentally, this third and latest type of rectifier is a continental development and has been manufactured in Europe within the International Telephone and Telegraph group of companies during most of the 1930's. Sufficient experience was gained in the technique of manufacture, as well as in methods of design and in selection of applications, to warrant engineering statements as to their general usefulness and importance to industry.

The purpose of this paper is to outline general principles of design of stacks, one or more of which make a rectifying unit. Starting with the availability of selenium plates or discs of various types, the engineer is confronted with the problem of

1. Selecting the proper size of plate to provide the required d-c output.

2. Computing the required a-c voltage to be impressed on the rectifier stacks to give the necessary d-c output.

3. Analyzing various factors such as type of load, nature of service, and cost of selected rectifier.

The paper includes dynamic and other characteristics of the principal sizes of selenium rectifier plates as applied to the design of single-phase, three-phase, full-wave, and half-wave rectifier units, and all variety of loading. Typical circuits are analyzed and novel representative computations of rectifier units are included.

Selenium Plates

THE rectification of alternating currents by means of the selenium rectifier takes place wholly within the constituent plate or plates, seven sizes of which are shown in Figure 1. The rectifying medium of this electronic device consists of selenium; the principle of operation is similar to that of other dry plate rectifiers, that is, low resistance in the forward direction, and high resistance in the reverse direction. A metal plate serves as back electrode; over it a layer of selenium is deposited, thin enough to give minimum internal losses, but sufficiently thick to withstand high inverse voltage. A soft metal of low-melting temperature is then applied over the selenium layer to form a front electrode. Finally, by means of controlled processes, a barrier layer is formed between the selenium and the front electrode.

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Selenium Rectifier Stacks

Rectifier stacks are produced by assembling selenium rectifier plates (40 plates are usually maximum) on a center stud with contact discs or washers interspersed between plates. Series and parallel arrangement can readily be provided as required, by inserting insulators between plates and introducing terminal lugs into the stack.

A stack consisting of 40 plates may be connected in several different ways: for example, as a bridge-circuit unit having ten plates in a series and one plate in parallel (4-10-1), or one plate in series and ten in parallel (4-1-10). The same stack may also be connected as a half-wave rectifier having all 40 plates in series to take care of voltage and one plate in parallel to take care of current (1-40-1). Further, the 40-plate stack may also be assembled as a doubler (2-10-2), two of which make a bridge-circuit rectifier with ten series and two parallel plates in each arm of the bridge.

For the three-phase half-wave circuit the total number of plates must be a multiple of three to allow a total connection such as 3-6-2 for a 36-plate single-stack unit. Similarly, this latter stack, if connected as 6-6-1, becomes either a bridge or center-tap three-phase rectifier.

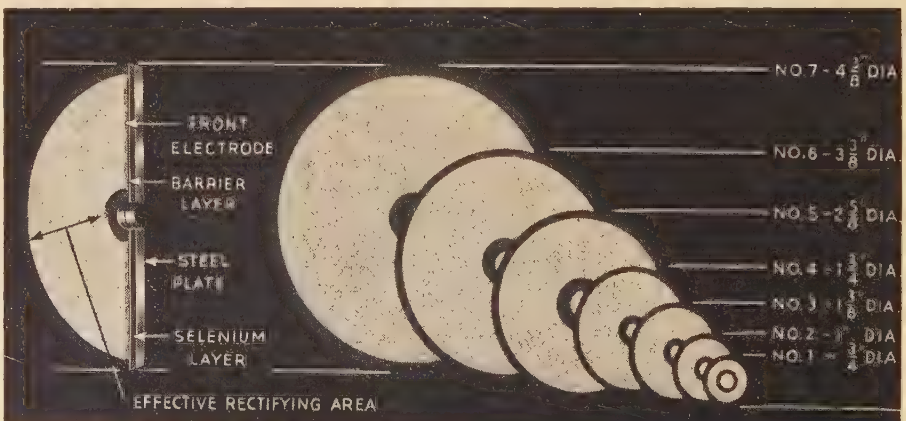
Rating of Plates

Current ratings of selenium plates are a function of their effective rectifying areas and heat-dissipating capacities. Table I lists seven basic plates, ranging from $^3/_4$ to $4^3/_8$ inches in diameter; it also shows ratings of the basic plates in various rectifier circuits. The ampere capacities shown in this table are based on an ambient temperature of 35 degrees centigrade and were determined experimentally. The requirements of a selenium rectifier in respect to current are always met by selecting the proper size of plates and their number in parallel, if the total rating of the rectifier exceeds the rating of an individual plate.

The ratings of the plates shown in Table I can be increased by providing additional cooling. Doubling the spacing between plates assembled in a single stack increases the heat-dissipating ability of the stack; the rating of plates is some 25 to 50 per cent higher than those shown in Table I. Table II lists six selenium plates having the same effective rectifying areas as those listed in Table I, but their current ratings are higher because of wider spacing.

By providing cooling fins, the forward current-carrying capacity of the plates can be raised still further. Table III contains eight additional plates with fins, the current ratings of which are from 2 to $2^1/_2$ times greater than the plates of the same rectifying areas listed in Table I. With extended ratings of the plates, whether by means of wide spacings or cooling fins, the internal losses of selenium rectifiers are greater; consequently, their efficiency is slightly reduced, and the voltage regulation is adversely affected.

Figure 1 (Below left). Cross section of a selenium rectifier plate showing the sequence of various layers
(Right) Seven basic sizes of selenium rectifier plates



Tables I, II, and III also give the maximum permissible reverse rms voltages per plate. The voltages thus indicated have ample safety margin. If, however, the reverse voltage be increased beyond safe limits, the rectifying layer between the selenium and the front electrode alloy breaks down. The maximum specified voltage must not, therefore, be exceeded. Tables I, II, and III include ratings of selenium plates in d-c circuits.

Rectifier stacks with narrow or wide spacing, as well as those with fins, are ordinarily cooled by convection. Large-size selenium rectifiers, however, often utilize forced draft ventilation in order to save space and to economize in rectifying elements. The normal plate rating can thereby be increased twofold and sometimes threefold. If, however, extended loading of plates with the aid of forced ventilation is not desired, substantially greater current output, as compared with free air conditions, can be obtained by mounting the stacks in a chimneylike enclosure (Figure 2). As will be noted from the figure, the least favorable condition is that of mounting the stacks in a cabinet provided with perforated covers.

The current rating of selenium rectifiers is limited only by the final plate temperature resulting from heating. The stacks can be heavily overloaded, provided the maximum safe operating temperature of 75 degrees centigrade is not exceeded. When this temperature is reached, either the load must be reduced to normal, or provision must be made for cutting the rectifying elements out of service as in the two half-wave rectifiers illustrated in Figure 3, widely used in business machines. Each element in these machines is equipped with a pair of bimetal strips connected by means of an adjustable screw. If, by chance, the key punch or the duplicator happens to be jammed and causes the temperature of the stacks to reach a range of 70 or 80 degrees centigrade, the bimetal

Table I. Current and Voltage Ratings of Seven Basic Selenium Plates When Used in Narrow Spacing Assemblies and With Resistive and Inductive Loads

For Battery-Charging and Capacitive Loads, These Ratings Are Reduced by 20 Per Cent. Conditions: Continuous Duty; 35 Degrees Centigrade Ambient Temperature

Plate Type No.	Diameter of Plates (Inches)	Maximum Number of Plates Per Stack	Maximum RMS Reverse Voltage Per Plate (Volts)	Single-Phase Rectifiers: Half-Wave (D-C Amperes)	Single-Phase Rectifiers: Bridge (D-C Amperes)	Single-Phase Rectifiers: Center-Tap (D-C Amperes)	Three-Phase Rectifiers: Half-Wave (D-C Amperes)	Three-Phase Rectifiers: Bridge (D-C Amperes)	Three-Phase Rectifiers: Center-Tap (D-C Amperes)	35 C Ambient Rating of Plates Used as D-C Valves: Amperes	35 C Ambient Rating of Plates Used as D-C Valves: Volts
1	$\frac{3}{4}$	36	18	0.04	0.075	0.075	0.10	0.11	0.13	0.06	15
2	1	36	18	0.075	0.15	0.15	0.20	0.225	0.27	0.12	15
3	$1\frac{3}{8}$	36	18	0.15	0.30	0.30	0.40	0.45	0.55	0.23	15
4	$1\frac{3}{4}$	40	18	0.30	0.60	0.60	0.80	0.90	1.1	0.45	15
5	$2\frac{5}{8}$	40	18	0.60	1.2	1.2	1.6	1.8	2.2	0.90	15
6	$3\frac{3}{8}$	40	16	1.2	2.4	2.4	3.2	3.6	4.5	1.8	12
7	$4\frac{3}{8}$	40	14	2.0	4.0	4.0	5.3	6.0	7.5	3.1	12

Table II. Current and Voltage Ratings of Six Selenium Plates (Similar to Table I, Except Number 1 Plate Omitted) When Used in Wide Spacing Assemblies and for Resistive and Inductive Loads

For Battery-Charging and Capacitive Loads, These Ratings Are Reduced by 20 Per Cent. Conditions: Continuous Duty; 35 Degrees Centigrade Ambient Temperature

Plate Type No.	Diameter of Plates (Inches)	Maximum Number of Plates Per Stack	Selenium Plate No. Used (See Table I)	Maximum RMS Reverse Voltage Per Plate (Volts)	Single-Phase Rectifiers: Half-Wave (D-C Amperes)	Single-Phase Rectifiers: Bridge (D-C Amperes)	Single-Phase Rectifiers: Center-Tap (D-C Amperes)	Three-Phase Rectifiers: Half-Wave (D-C Amperes)	Three-Phase Rectifiers: Bridge (D-C Amperes)	Three-Phase Rectifiers: Center-Tap (D-C Amperes)	35 C Ambient Rating of Plates Used as D-C Valves: Amperes	35 C Ambient Rating of Plates Used as D-C Valves: Volts
20	1	28	2	18	0.11	0.22	0.22	0.29	0.33	0.4	0.17	15
21	$1\frac{3}{8}$	28	3	18	0.23	0.45	0.45	0.6	0.67	0.82	0.34	15
10	$1\frac{3}{4}$	28	4	18	0.39	0.78	0.78	1.0	1.1	1.4	0.58	15
11	$2\frac{5}{8}$	28	5	18	0.78	1.6	1.6	2.1	2.3	2.8	1.2	15
14	$3\frac{3}{8}$	28	6	16	1.5	3.1	3.1	4.1	4.6	5.8	2.4	12
18	$4\frac{3}{8}$	28	7	14	2.6	5.2	5.2	6.9	7.8	9.7	4.0	12

Table III. Current and Voltage Ratings of Eight Selenium Plates (Numbers 4, 5, 6, and 7) Equipped With Cooling Fins of Different Sizes, and Used for Resistive and Inductive Loads

For Battery-Charging and Capacitive Loads, These Ratings Are Reduced by 20 Per Cent. Conditions: Continuous Duty; 35 Degrees Centigrade Ambient Temperature

Plate Type No.	Size of Cooling Fins (Inches)	Maximum Number of Plates Per Stack	Selenium Plate No. Used (See Table I)	Maximum RMS Reverse Voltage Per Plate (Volts)	Single-Phase Rectifiers: Half-Wave (D-C Amperes)	Single-Phase Rectifiers: Bridge (D-C Amperes)	Single-Phase Rectifiers: Center-Tap (D-C Amperes)	Three-Phase Rectifiers: Half-Wave (D-C Amperes)	Three-Phase Rectifiers: Bridge (D-C Amperes)	Three-Phase Rectifiers: Center-Tap (D-C Amperes)	35 C Ambient Rating of Plates Used as D-C Valves: Amperes	35 C Ambient Rating of Plates Used as D-C Valves: Volts
9	$2\frac{5}{8}$ Diameter	28	4	18	0.58	1.1	1.1	1.5	1.7	2.1	0.87	15
12	$3\frac{3}{8}$ Diameter	28	5	18	0.90	1.8	1.8	2.4	2.7	3.3	1.4	15
13	$4\frac{3}{8}$ Diameter	28	5	18	1.1	2.2	2.2	2.9	3.3	4.0	1.7	15
15	$4\frac{5}{8}$ Diameter	28	6	16	1.8	3.5	3.5	4.6	5.2	6.5	2.7	12
16	$4\frac{3}{8}$ Diameter	24	6	16	1.9	3.8	3.8	5.0	5.6	7.0	2.9	12
17	6 x 6	28	6	16	2.7	5.4	5.4	7.2	8.1	10.0	4.1	12
19	6 x 6	28	7	14	3.7	7.4	7.4	9.8	11.1	13.3	5.7	12
8	8 x 8	28	7	14	5.0	10.0	10.0	13.0	15.0	18.0	7.5	12

strips separate, thus breaking the circuit. The plates then cool off and the bimetal strips again close (Figure 4).

Intermittent Service

Considerable gain in the current capacity of selenium rectifiers can be obtained when they are used in intermittent service, in which case, the duty cycles must be definitely established. The variety of these intermittent applications is great, and their complete discussion here would be too lengthy. One formula, however, is frequently used for periodic loadings:

$$I_m = I_{\max}\sqrt{\frac{A}{A+P}} \tag{1}$$

where I_m is the continuous current rating, $I_{\max}$ the maximum current drawn periodically, A the operating period, and P the inoperative interval; both A and P should be in the same units. Experience has shown that this formula can be used only if A is less than the selenium plate time constant T, which may be defined by:

$$t_1 = t_2(1-e^{\frac{A}{T}}) \tag{2}$$

and which varies from five to eight minutes, depending on the plate size (t_1 and t_2 are instantaneous and final plate temperatures, respectively). When operating periods are separated by inoperative intervals of such length that the rectifier again cools practically to normal ambient temperature, much greater plate overloads are practicable.

Design of Stacks by Direct Values

After choosing the size of plate with proper current-carrying capacity, the internal voltage drop of the plate is considered. Figure 5 illustrates the average experimentally determined internal voltage drop for seven plates in either bridge or center-tap single-phase circuits. The

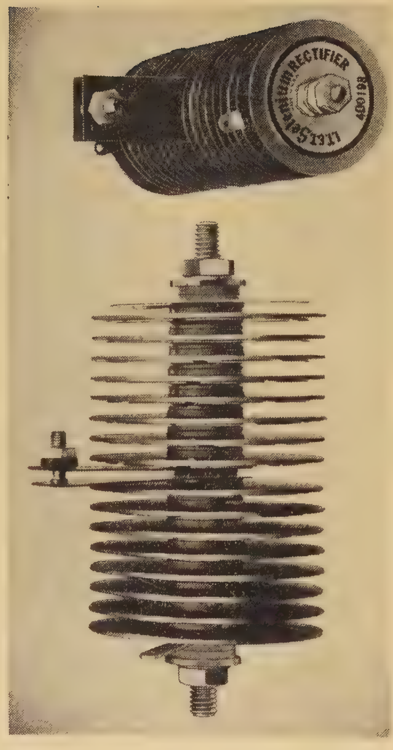


Figure 3. Two half-wave selenium rectifiers for operation of magnets in business machines

Both stacks are equipped with bimetal strips to open the circuit when the plate temperature exceeds a specified value

voltage drop per plate, designated as dv, is one half of the difference between the rms values of voltages read on the input and output side of the rectifier. These quantities are plotted as ordinates against the arithmetical values of output current in amperes plotted as abscissa.

The output voltage of a selenium rectifier is determined by the input voltage V_{ac} less the total voltage drop within the rectifier. The computation of the necessary a-c voltage to be impressed on the selenium rectifier involves consideration of the voltage drop per plate and the number of plates through which the current flows.

Using the data given in Tables I, II,

Table IV. Selenium-Rectifier Design Constants: k_1 = Form Factor; n = Number of Plates in Series; k_2 = Circuit Factor; V_p = Maximum Voltage Per Plate; V_{ac} = Phase Voltage, Except Three-Phase Bridge Where It Is Line Voltage

Number of Phases	Circuit Type	k_1	n	k_2
1	Half-wave	2.3	$\frac{V_{ac}}{V_p}$	1
1	Bridge	1.15	$\frac{V_{ac}}{V_p}$	2
1	Center-tap	1.15	$\frac{2V_{ac}}{V_p}$	1
3	Half-wave	0.855	$\frac{\sqrt{3}V_{ac}}{V_p}$	1
3	Bridge	0.74	$\frac{V_{ac}}{V_p}$	2
3	Center-tap	0.74	$\frac{2V_{ac}}{V_p}$	1

and III, and the internal voltage drop per plate illustrated in Figure 5, any single-phase selenium rectifier can be designed and the necessary a-c voltage computed by the following formula:

$$V_{ac} = k_1 V_{dc} + k_2 n \text{ dv} \tag{3}$$

where V_{ac} is the input voltage, k_1 the form factor to convert the arithmetical value to the rms voltage value (Table IV), V_{dc} the required d-c output voltage, k_2 the number of arms through which the current must pass in the circuit for each half-cycle, n the number of plates in series per arm, and dv the voltage drop per plate for the circuit employed. The constants k_1 and k_2 vary depending upon whether a bridge or center-tap connection, a single or three-phase circuit is employed.

As an example, let us design a bridge-type unit required to deliver four amperes at 16 volts continuously under the maximum ambient temperature of 35 degrees centigrade in a single-phase circuit. Selenium plate 7, $4^3/_8$ inches in diameter

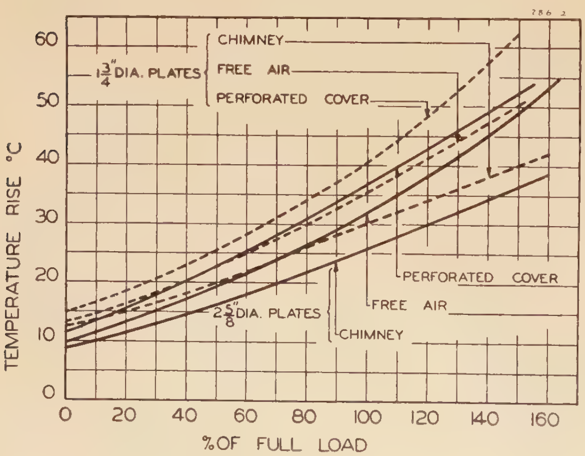


Figure 2. Temperature characteristics of two selenium rectifiers under three different cooling conditions

1. In a cabinet with perforated top and bottom covers
2. In free air
3. In a chimney-like enclosure fully open at the top and bottom

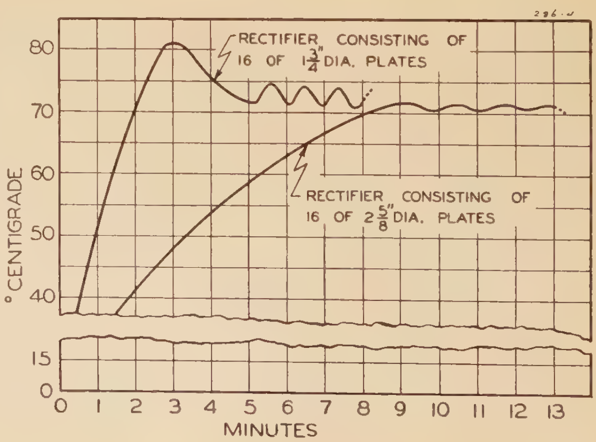


Figure 4. Time-temperature characteristics illustrating the performance of selenium rectifiers equipped with bimetal strips (Figure 3)

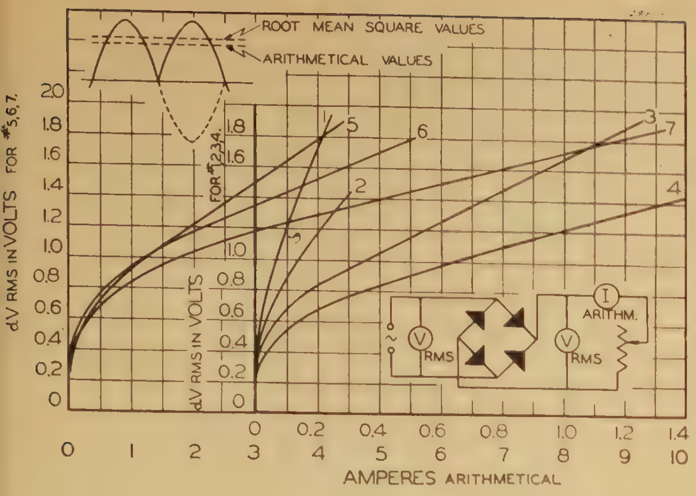


Figure 5. Rectification characteristics of seven basic plates ($^3/_4$, 1, $1^3/_8$, $1^3/_4$, $2^5/_8$, $3^3/_8$, and $4^3/_8$ inches in diameter) used in the design of single-phase bridge and center-tap rectifiers for inductive and resistive loads

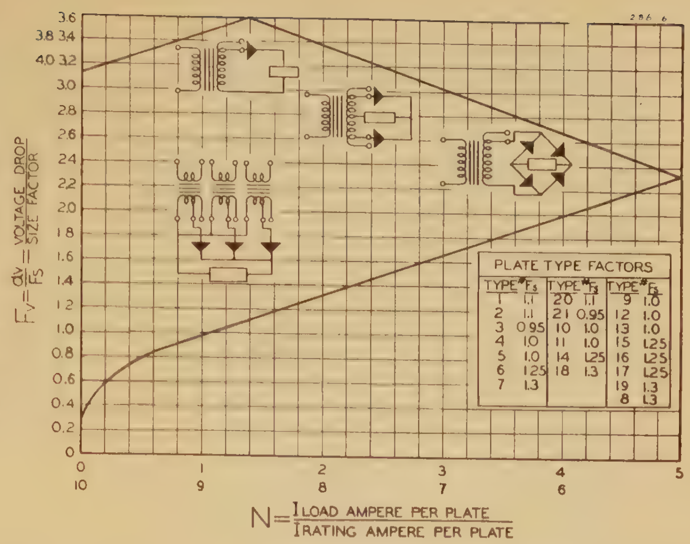


Figure 6. Characteristic illustrating the relation of F_v and N for single-phase half-wave, center-tap, and bridge rectifier circuits with resistive or inductive load; also for three-phase half-wave rectifier circuit with all types of load

(listed in Table I and rated at four amperes) will serve the purpose. Using equation 3 and corresponding constants from Table IV:

$V_{ac} = 1.15 \times 16 + 2n \text{ dv}$

The number of plates in series, that is, quantity n, necessary at this stage, can be computed by the following formula:

$$n = \frac{k_1 V_{dc}}{V_p - 2 \text{ dv}} \tag{4}$$

V_p for plate 7 is 14, and dv, as read off characteristic 7 in Figure 5, is 1.29; hence

$$n = \frac{1.15 \times 16}{14 - 2 \times 1.29} = 1.6 \text{ or 2 of number 7 plates in series}$$

V_{ac}, therefore, at the start of service, is equal to 23.6 volts. With the aging of the selenium rectifier, the forward resistance increases, and the inverse resistance also slightly increases. Based on lengthy experience in the design and application of these rectifiers, it can be stated that the only variable quantity of equation 3 is dv, which may increase as much as 50 per cent under most adverse conditions. An additional tap, therefore, should be provided in the transformer winding to give the V_{ac} required for the aged condition:

$V_{ac} = 1.15 \times 16 + 2 \times 2 \times 1.29 \times 1.5 = 26$ volts

Designs by Relative Values

The foregoing example and reference to Figure 5 characteristic apply only to the single-phase bridge and center-tap designs with either inductive or resistive loads. With capacitive and battery-charging loads, even in the above-mentioned circuits, intermittent loading on the selenium plates occurs with periodic values of the forward current greater than the periodic values for the same d-c output feeding resistive or inductive load; voltage drops per plate are, therefore, greater in the former case. Applications occur, however, where the voltage drop per plate is smaller than the values shown in Figure 5. An example is the three-phase circuit where the rectified current is practically at the peak value of the applied alternating current. The output current density per plate in these circuits is considerably higher than in the case of a single-phase bridge circuit; furthermore, the type of load, whether it be resistive, inductive, capacitive, or battery charging, has practically no effect on the voltage drop per plate.

Inasmuch as a wide variety of circuits and types of loading is encountered, a

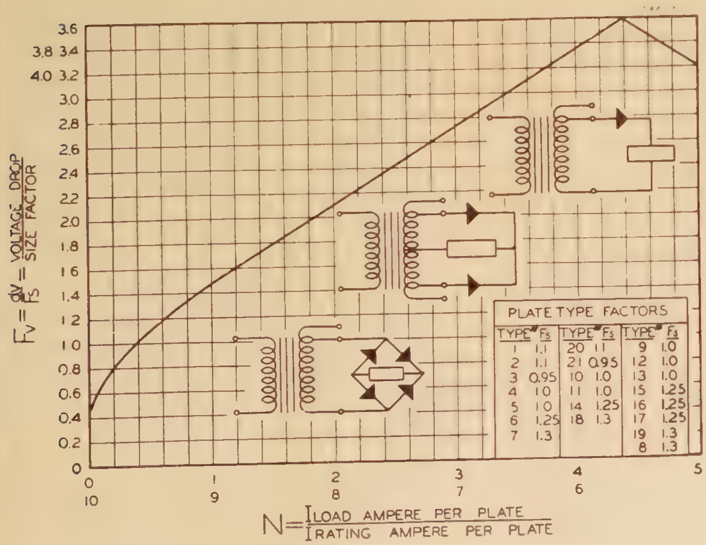


Figure 7. Characteristic illustrating the relation of F_v and N for single-phase half-wave, center-tap, and bridge rectifier circuits for battery-charging application or capacitive loads

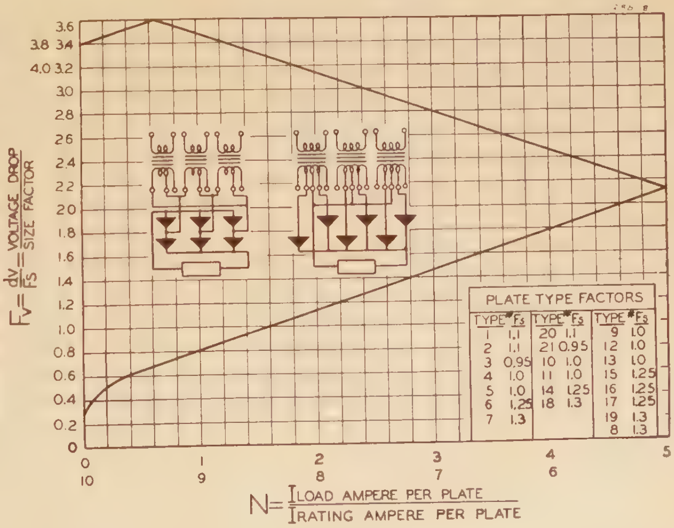


Figure 8. Characteristic illustrating the relation of F_v and N for three-phase center-tap and bridge rectifier circuits for all types of loads

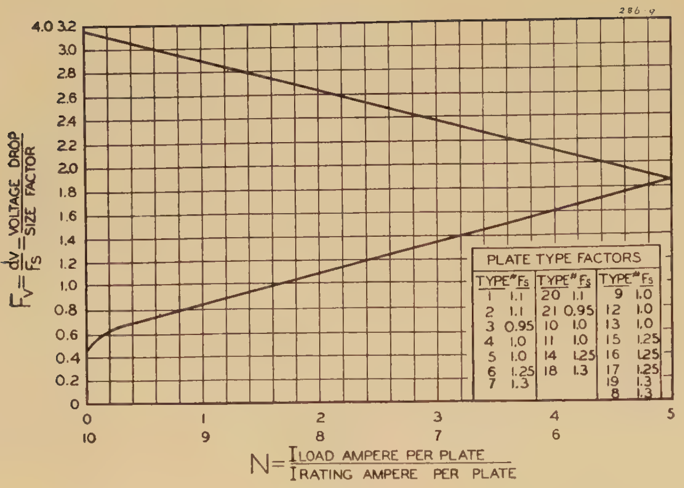


Figure 9. Characteristic illustrating the relation of F_v and N for d-c and blocking circuits

method for rating the 21 rectifier types according to relative values of output current and voltage drop per plate has been developed. Figures 6, 7, 8, and 9 illustrate the relationships N and F_v for various types of circuits and loads.

The use of these characteristics involves first, determination of the value N, which is obtained by dividing the actual ampere load per plate by the ampere rating of the basic plate employed. With this value determined, the value of F_v is read from one of the characteristics and then multiplied by the plate size factor F_s to obtain the actual voltage drop per plate, dv for the plate selected.

Figure 6 illustrates the relationship of F_v to the value N for half-wave, center-tap, and bridge single-phase circuits when loaded with either resistive or inductive load; also for half-wave three-phase circuits for all types of loads. Figure 7 gives a similar relationship for F_v to the value N for half-wave, center-tap, and bridge circuits, all single-phase for capacitive or battery-charging applications. Figure 8 shows the relationship of F_v to N for three-phase bridge and center-tap circuits for all types of loads. Finally, Figure 9 shows the relationship of F_v to N for d-c and blocking circuit applications.

To illustrate this method of design, let us compute a three-phase selenium rectifier capable of delivering 325 amperes at 13 volts for the filament supply of a television transmitter tube. An additional requirement is that the rectifier deliver not more than 488 amperes at approximately two volts into tube when cold.

Several plates in Tables I, II, and III should be tried; however, plate 13, rated at 3.3 amperes, will be most economical; the total current of 325 amperes can be safely handled by 100 plates connected in parallel.

$$I_{dc} = \frac{325}{100} = 3.25 \text{ amperes per plate}$$

This plate loading with 1.8 ampere rating for basic plate 5 used in type 13 will give

$$N = \frac{3.25}{1.8} = 1.8$$

From Figure 8, $F_v = 1.07$ and $dv = 1.07 \times 1 = 1.07$.

In aging, the dv value may increase 50 per cent, and thus becomes 1.6. Substituting the known quantity in formula 4:

$$n = \frac{0.74 \times 13}{18 - 2 \times 1.6} = 0.65; \text{ or 1 plate in series}$$

The foregoing proves that the entire rectifier should consist of a total connection of (6-1-100), where the first number (6) designates the number of arms of the three-phase circuit, the second number (1) indicates the number of plates in series, and the third number (100) gives the number of plates in parallel. The total of 600 plates of type 13 may be conveniently assembled into 24 stacks, each having 25 plates in parallel.

Using formula 3 and the design constants of Table IV:

$$V_{ac} = 0.74 \times 13 + 2 \times 1 \times 1.07 = 11.8 \text{ volts}$$

when rectifying elements are new;

$$V_{ac} = 0.74 \times 13 + 2 \times 1 \times 1.6 = 12.8 \text{ volts}$$

when they are fully aged.

In order to meet the requirements of approximately two volts with the current output of 488 amperes:

$$I_{dc} = \frac{488}{100} = 4.88 \text{ amperes per plate}$$

$$N = \frac{4.88}{1.8} = 2.7$$

From the characteristic of Figure 8

$F_v = 1.37$

For the new stacks, therefore, $dv = 1.37 \times 1 = 1.37$, and for fully aged stacks

$dv = 1.37 \times 1.5 = 2.06$

Using formula 3 and these two values of dv, the new and aged V_{ac} will be found to be 4.2 and 5.6 volts, respectively. The 50 per cent limit for the current requirement is actually met by the external limiting reactances in each phase of the three-phase circuit. The complete assembly of this rectifier is shown in Figure 10.

The ordinary single-phase bridge-type rectifier for resistive loading can also be designed through the use of relative values. Figure 11 illustrates a two-ampere, 120-volt telautograph unit.

With two number 9 plates in parallel, or one ampere per plate:

$$N = \frac{1}{0.6} = 1.67$$

From the characteristic of Figure 6

$F_v = 1.2$ $\qquad dv = 1.2 \times 1.0 = 1.2$

Hence

$$n = \frac{1.15 \times 120}{18 - 2 \times 1.2 \times 1.5} = 9.6, \text{ or 10 plates in series}$$

The total connection of the rectifier is then 4-10-2. Practical considerations suggest four stacks, two of which make one bridge with ten plates in each arm of the bridge. Again, from formula 3, corresponding constants of Table IV and the above dv value, V_{ac} is found to be 162 volts when new and 174 volts when aged.

Ratings (Tables I, II, and III) of selenium rectifier plates, functioning as blocking valves in d-c circuits, are higher in current and lower in voltage value than they are in half-wave alternating circuits. The higher current rating is acceptable, inasmuch as the forward resistance ordinarily decreases when only forward current passes through the selenium rectifier plates. The reverse current of the blocking unit, on the other hand, is higher, and the safe voltage limit is, therefore, more conservatively established than for a-c circuits. As an example, a 30-volt, 4.5-ampere blocking unit consists of a total connection of 1-2-5 number 5 plates. The value N for this unit is equal to one, and dv is 0.84 (Figure 9).

Experience has shown that for constant-current battery-charging and capacitive loading, the current rating should be only

Table V. Relation of Factor k_3 by Which Normal Rating of Number 7 (That Is, $4^3/_8$-Inch-Diameter) Selenium Plates Can Be Increased, and the Speed and Amount of Air Necessary

Multiplying factor for normal plate ratings, k_3	1	1.5	2	2.5	3	3.5	4	4.5
Air speed in feet per minute	0	60	90	120	160	200	310	400
Cubic feet per minute per plate	0	0.5	0.8	1.1	1.4	1.7	2.8	3.7

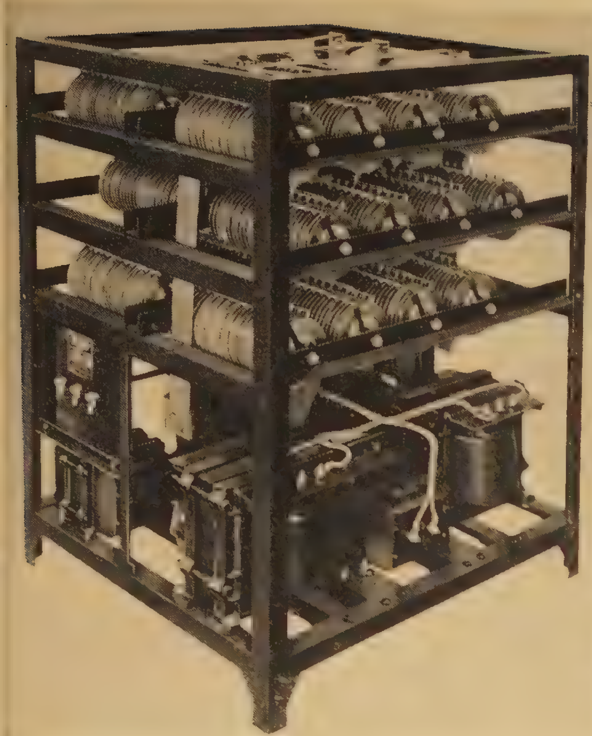


Figure 10 (left). Rectifier unit of 325-ampere 13-volt rating with reactances limiting output current to 488 amperes at two volts

A-c input: 220 volts, three phases, 60 cycles. Rectifying element consists of 600 plates arranged in 24 stacks, each stack consisting of 25 plates in parallel

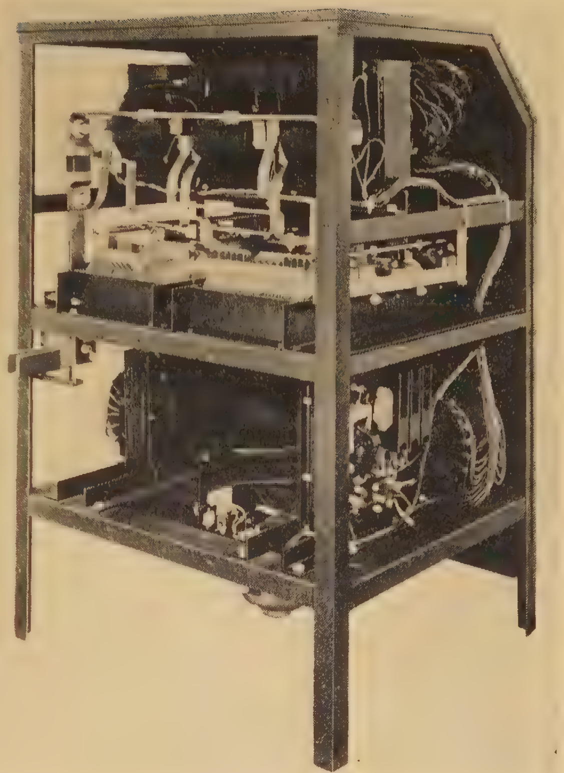


Figure 12 (right). Three-phase selenium rectifier unit employing forced-draft ventilation for output of 600 amperes at six volts

Over-all efficiency: 74 per cent; power factor: 94 per cent

80 per cent of the values tabulated in Tables I, II, and III. In the design of the rectifier for charging a 60-cell battery at the rate of 0.4 ampere, with 2.4 volts per cell, plate 4 may be selected. The value of N is 0.67. The new dv, as read off the characteristic of Figure 7, is 1.25 and, after aging, it becomes 1.87. The number of plates in series is determined either by

$$n=\frac{V_b}{V_p} \tag{5}$$

$$\text{or } n=\frac{V_b/\sqrt{2}}{V_p-2\text{ dv}} \tag{6}$$

depending on which is greater.

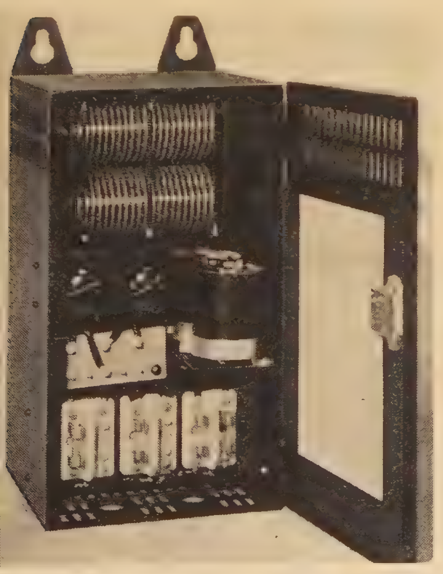


Figure 11. Selenium rectifier with output of two amperes at 120 volts for powering telautograph equipment

$$V_{ac}=\frac{V_b}{\sqrt{2}}+k_2n\text{ dv} \tag{7}$$

$$=\frac{60\times 2.4}{\sqrt{2}}+2\times 8\times 1.25=122\text{ volts}$$

An additional tap to give 132 volts for the fully aged condition of the stacks should be provided.

Design of Forced-Draft-Ventilation Unit

The extended current rating of selenium rectifier plates with forced draft or fan cooling and the speed of air constituting the forced ventilation require further consideration. A rather conservative relationship of current rating to air velocity has been established and successfully used for the $4^3/_8$-inch.-diameter number 7 plate (Table V).

As an example, let us design a three-phase full-wave rectifier supplying a d-c output of 600 amperes at six volts. Because of the low required output voltage, as compared to the full rms voltage permissible for the number 7 plate, the center-tap circuit is most economical and gives greatest efficiency. With the fan delivering air at a speed of 120 feet per minute, the 7.5-ampere (Table I) loading of this plate can be increased 2.5 times (Table V), thus making it 18.7 amperes per plate. Practical consideration of possible 10 per cent overload suggests that this unit should have 36 plates in parallel for the total current output of the unit. This makes the value N (Figure 8) equal to 2.22, and the initial and aged dv equal to 1.6 and 2.4 respectively. The total connection of the rectifier is, therefore, 6-1-36. The new V_{ac} for half of the transformer secondary voltage is 6.1 volts, and for the fully aged condition 6.9 volts. A view of this equipment is shown in Figure 12.

Voltage Regulation

The inherent voltage regulation of the selenium rectifier is in the neighborhood of 10 to 20 per cent. In computing the regulation, one must determine the no-load

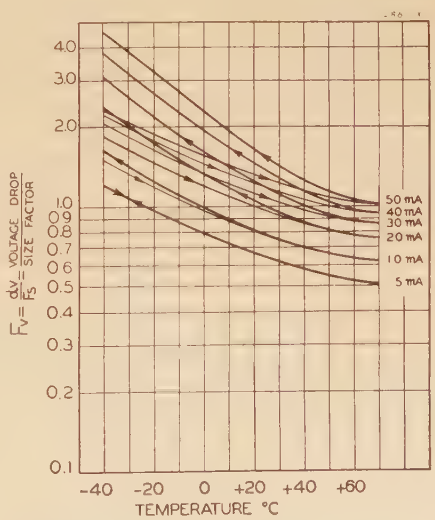
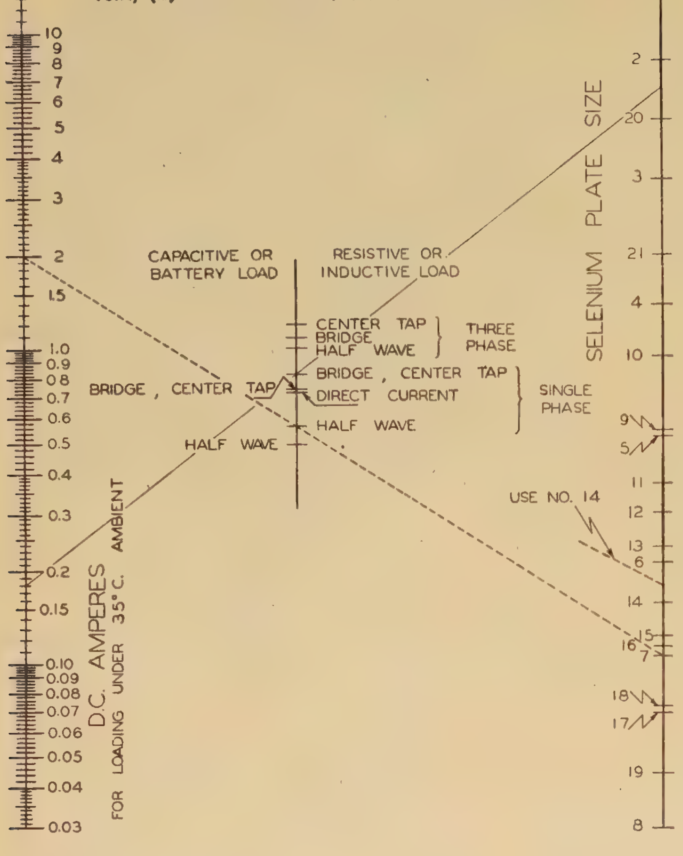
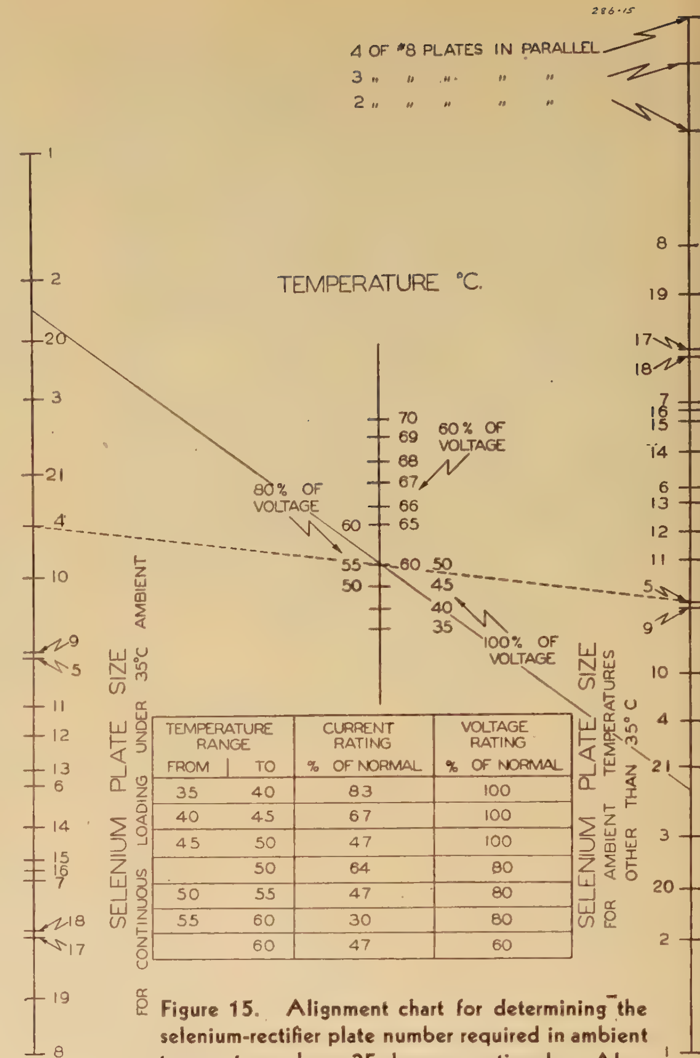


Figure 13. Relation of F_v at varying current densities to the ambient temperatures throughout a heating and cooling cycle

Figure 14. Alignment chart for determining the rectifier type number for (1) output current, (2) nature of load, (3) type of circuit





TEMPERATURE RANGE FROM	TEMPERATURE RANGE TO	CURRENT RATING % OF NORMAL	VOLTAGE RATING % OF NORMAL
35	40	83	100
40	45	67	100
45	50	47	100
	50	64	80
50	55	47	80
55	60	30	80
	60	47	60

Figure 15. Alignment chart for determining the selenium-rectifier plate number required in ambient temperatures above 35 degrees centigrade. Also, table of percentage of current and voltage ratings at ambient temperatures above 35 degrees centigrade

value of dv and then the no-load output voltage. In the case of the first example of a four-ampere, 16-volt rectifier, the dv value from Figure 5 is 0.4. The d-c output voltage, therefore, at no load is

$$V_{dc}=\frac{23.6-2\times2\times0.4}{1.15}=19.1$$

$$\text{Regulation}=\frac{19.1-16}{16}\times100=19.4 \text{ per cent}$$

Similarly, the regulation of the three-phase 325-ampere 13-volt unit (Figure 10) is computed by reading F_v for N equal to zero in Figure 8. Substituting 0.3 for the value of dv in formula 3, and taking 11.8 volts for V_{ac}, one finds the no-load voltage to be 15 volts, and the voltage regulation is, therefore, 15.4 per cent.

Efficiency

The efficiency of selenium rectifiers varies with the type of circuit and the nature of the load. The single-phase circuits with fully loaded plates in respect to voltage and current, and when feeding either resistive or inductive loads, give an efficiency of approximately 64 per cent; the same circuits, when used for battery charging give an efficiency some 14 per cent higher due to the greater value of the rectified voltage. The three-phase circuits give efficiency values in the neighborhood of 83 per cent and, for all practical purposes, remain the same irrespective of the type of load. For all circuits and loads, however, the efficiency of selenium rectifiers increases with decrease of load down to approximately 25 per cent of full value, and thereafter falls off rapidly.

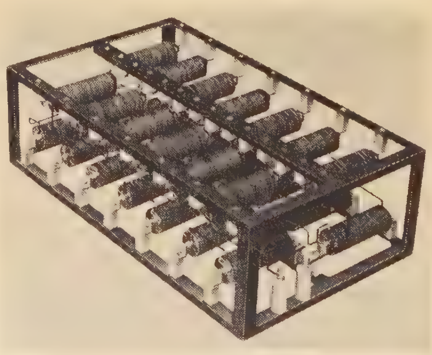


Figure 16. Single-phase bridge-connected rectifier

Output: 180 milliamperes at 2,300 volts under ambient temperatures from −40 to +60 degrees centigrade

The efficiency itself depends on the combined losses in the selenium rectifier from forward and reverse currents, and, in formula form, it is:

$$\frac{V_{dc}I_{dc}}{(V_{dc}I_{dc})+W_f+W_r}\times100=\%\text{ efficiency} \quad (8)$$

where W_f are losses due to forward current, and W_r are losses due to the reverse current.

The computation of exact efficiency of all sizes of plates and various loads and circuits is rather involved and constitutes an extensive subject in itself.

In order to illustrate the simplified method of efficiency computation and the effect of dv changes on its value, let us compute the efficiency of the 325-ampere 13-volt three-phase unit illustrated in Figure 10.

Forward losses per plate in the three-phase bridge circuit are

$$w_f=\frac{\sqrt{2}I_{dc}\,\text{dv}}{3}$$

where $\sqrt{2}$ is a conversion factor for approximating the peak value of the a-c

wave in terms of effective value of dv. Divisor 3 results from the fact that in three-phase bridge circuit each plate is utilized $2\times 1/6$ times in each cycle.

$$w_f = \frac{325/100\times\sqrt{2}\times 1.07}{3} = 1.64 \text{ watts per plate}$$

or $W_f = 6\times 100\times 1.64 = 984$ watts for all 100 plates in six arms.

The reverse losses are approximately one third of forward losses computed for the normal rating of the plate used in this unit

$$W_r = \frac{1}{3}\times\frac{6\times 100\times 0.8\times\sqrt{2}\times 1.8}{3} = 136 \text{ watts}$$

The new input wattage W, therefore, is

$$W = (I_{dc}\times V_{dc}) + W_f + W_r$$
$$= (325\times 13) + 984 + 136 = 5{,}345 \text{ watts}$$

$$\text{Efficiency} = \frac{4{,}225}{5{,}345}\times 100 = 79.2 \text{ per cent}$$

Similarly, assuming a possible 50 per cent change in dv, the efficiency of this rectifier with stacks fully aged is computed as 72 per cent.

The foregoing designs illustrate the importance of the quantity dv, which is dependent on the forward resistance and current density of the plates, as well as the ambient temperature under operating conditions. Its changing values under varying conditions greatly influence the efficiency, regulation, and aging of selenium rectifiers. The ambient temperature and current density relationships affecting the value of dv are illustrated in Figure 13. The arrows on the curves indicate that the resistance of the plates decreases with increase of temperature. As the plates cool off, the resistance again increases, and dv is greater at the new lower temperatures than during the rising-temperature phase of the heating cycle. This phenomenon diminishes with lower current densities to a point where the resistance is the same at corresponding temperatures of the heating and cooling portions of the cycle.

Selection of Plate Type for 35 Degrees Centigrade and Higher Ambient Temperature

Almost invariably more than one selenium plate type appears suitable for given output requirements. The type of circuit or nature of loading, as well as the cost, however, restricts the choice.

The alignment chart (Figure 14) has been found useful in selecting plate types for specified output currents. If a straight edge is laid connecting the required d-c output with the type of circuit and the nature of load, the intersection of the straight edge on the plate size scale gives the type number of the required rectifier plate. If the intersection falls between two plate type numbers, the plate having the higher current rating should be chosen.

For ambient temperatures higher than 35 degrees centigrade, the plate type number for a 35 degrees centigrade ambient should first be found. Referring to Figure 15, a straight edge connecting the 35 degrees centigrade ambient temperature plate type number with the desired higher ambient value of the temperature scale intersects the right-hand scale, indicating the required higher ambient plate type number. Again, if the intersection is between two plate type numbers, the plate type number with the higher current rating should be used. It will be noted that, by decreasing the voltage rating, a small increase in current rating is allowable.

As an example, let us design a high-voltage low-current rectifier of the type illustrated in Figure 16 for either resistive or inductive load. Referring to Figure 14, the line drawn through the 0.18 reading on the left-hand scale, and the point marked "single-phase bridge" in the middle of the chart, intersects the right-hand scale between "2" and "20." Thus, plate 20 (Table II) would be used if the rectifier is to operate under maximum ambient temperature of 35 degrees centigrade. In order to derate plate 20 for a 60 degrees centigrade ambient, reference is made of Figure 15. The line drawn on this chart through the same point (between "2" and "20") of the left-hand scale (as in Figure 14) and the point marked "60 per cent of voltage" indicates that the number 21 selenium plate (Table II) should be used for the required assembly. Further computations of quantities N, dv, and n result in the design of a rectifier with total connections of 4-224-1, arranged in 28 stacks, each consisting of 32 of $1^3/_8$-inch plates in series.

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Rectifier Terminology and Circuit Analysis

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ANYONE reading several articles on the subject of rectifiers must be impressed by the confusion caused by a lack of exact terminology. This paper presents definitions of a few terms representing the most important concepts arising in the rectifier field. The definitions are presented with the hope that they will be found sufficiently useful to be widely adopted. Constructive criticism of the terms and definitions is invited.

These terms have been defined to conform with the general practice in the power-rectifier field. It is to be hoped, however, that the concepts will be sufficiently broad and accurate to be generally useful in all fields where rectifiers are employed.

In many of the definitions the term "circuit element" is employed. This has not been defined, because it is in general use in electrical literature and has been defined elsewhere.[1] The definitions suggested are given below.

Rectifier Terms

1. *Rectifying Element.* A rectifying element is that circuit element which has the property of conducting current effectively in only one direction.

In arc rectifying devices, the portion of the circuit including an anode, its cathode, and the arc space between the anode and cathode, is the rectifying element.

When a group of rectifying units is connected, in either parallel or series arrangement, to operate as one circuit element, the group of rectifying units should be considered as a rectifying element.

2. *Double-Way Rectifier* (*Double-Direction Rectifier*). A double-way rectifier is a rectifier in which all parts of the alternating-voltage circuit elements, conductively connected to the rectifying elements, conduct current in both directions (that is, in alternate half-cycles).

3. *Single-Way Rectifier* (*Single-Direction Rectifier*). A single-way rectifier is one in which no portion of the alternating-voltage circuit elements, conductively connected to the rectifying elements, conducts current in both directions (that is, in alternate half-cycles) because of the action of the rectifying elements.

4. *Simple Rectifier.* A simple rectifier is a rectifier in which the total direct current at any instant, exclusive of the commutating time, flows through a single rectifying element for a single-way rectifier, or flows through two rectifying elements in series (one on either side of the a-c circuit element) for a double-way rectifier.

5. *The Number of Phases in a Simple Rectifier.* The number of phases in a *simple single-way* rectifier is equal to the number of rectifying elements.

The number of phases in a *simple double-way* rectifier is equal to half the number of rectifying elements.

A double-way rectifier may have a primary-current wave shape and a d-c ripple voltage corresponding to a single-way rectifier having twice the phases of the double-way rectifier.

In describing a *simple rectifier*, three items of information are usually necessary and sufficient:

(*a*). The number of phases of the rectifier.

(*b*). Classification as either single way or double way.

(*c*). The transformer-circuit description if transformers are used.

Example: A three-phase single-way rectifier using delta zigzag wye transformers.

6. *Multiple Rectifier.* A multiple rectifier is one in which two or more similar simple rectifiers are connected in such a way that their direct currents add, but their d-c voltage ripples do not coincide. Interphase transformers are usually required between the component simple rectifiers in a multiple rectifier.

When two or more groups of rectifying elements operate so that their d-c currents add and their d-c voltage ripples coincide, the groups of rectifying elements are in parallel.

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In describing a multiple rectifier, four items of information are usually given, and a fifth, the classification as single-way, is usually assumed. The information expressly stated is:

(*a*). The primary wave shape, or the number of phases in a simple single-way rectifier to give the same primary wave shape.

(*b*). The number of component simple rectifiers in the multiple rectifier.

(*c*). The number of phases in a component simple rectifier.

(*d*). The transformer-circuit description if transformers are used.

Example: a 12-phase quadruple wye (single-way) rectifier, using delta zigzag wye transformers.

7. *Cascade Rectifier.* A cascade rectifier is a rectifier in which two or more similar simple rectifiers are connected in such a way that their d-c voltages add.

8. *Commutating Reactance.* The commutating reactance is the reactance which effectively opposes normal currens transfer between rectifying elements which conduct consecutively.

In the actual rectifier circuit this reactance is usually distributed in various parts of the circuit; for example, the anode leads, the transformer windings, and the a-c supply system. In many cases these reactances may be considered as lumped in the transformer secondary.

9. *Reactance Factor* (*Commutating-Reactance Factor*). The commutating-reactance factor is the per-unit commutating reactance calculated on the arbitrary base of the rms value of the secondary line-to-neutral voltage divided by the direct current commutated.

Particular attention is called to the terms "double-way" rectifier and "single-way" rectifier. These terms are suggested to replace the terms "half-wave" and "full-wave" as frequently employed. There are two arguments against the old terms. In the first place, they have been used by two groups in different ways. For example, the circuit shown in Figure 1 is sometimes described as a "full-wave" circuit. It is also described as a "half-wave" circuit by a substantial group.

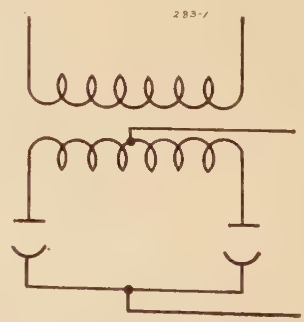

Figure 1. Single-way rectifier circuit

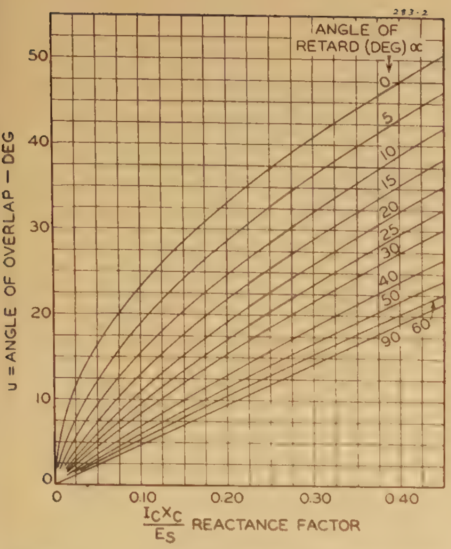

Figure 2. Angle of overlap—characteristic curves. $P = 3$

$$\cos(u+\alpha) = \cos\alpha - \frac{I_cX_c}{\sqrt{2}E_s \sin\frac{\pi}{P}}$$

when $\alpha=0$

$$\cos u = 1 - \frac{I_cX_c}{\sqrt{2}E_s \sin\frac{\pi}{P}}$$

With this background of confusion a new term seems the only solution. A further objection to the terms "full-wave" and "half-wave" is the fact that they are accurately descriptive only in the case of 180-degree conduction, which is not widely used in power rectifiers. The terms "double-way" and "single-way" describe accurately the physical difference which separates the two classes of rectifiers and results in different general relations for several current and voltage ratios.

The "simple rectifier" is an essential step in the development of the more complicated rectifier circuits, and it seems necessary to employ this term.

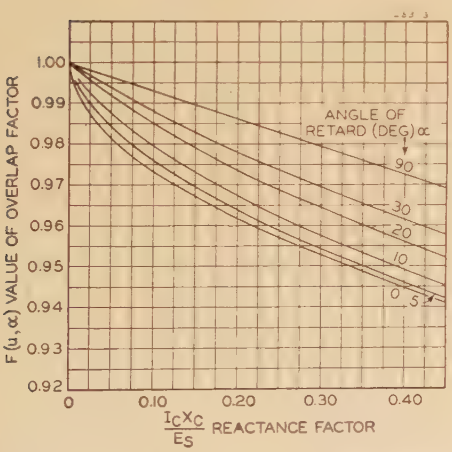

Figure 3. Overlap factor—characteristic curves. $C = 3$

$$F(u,\alpha) = \sqrt{1-Cf(u,\alpha)}$$

when $\alpha=0$

$$F(u) = \sqrt{1-Cf(u)}$$

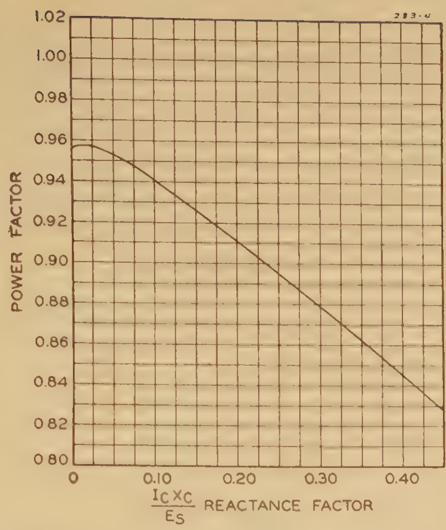

Figure 4. Power factor—characteristic curve for delta double-wye rectifier. $P=3$, $C=3$

$$PF = \frac{3}{\pi}\cdot\frac{1}{\sqrt{1-3f(u)}}\cdot\left(1-\frac{E_x}{E_{do}}\right)$$

The definition of the number of phases as given is required if a general relation for the d-c voltage is to be employed. This definition is logical and consistent. Unfortunately the biphase rectifier as here defined has frequently been classed as a single-phase rectifier.

The "multiple rectifier" is here restricted to the case of several simple rectifiers operating in parallel in such a way that their d-c voltage ripples do not coincide. This is a unique arrangement in rectifier practice which improves the operation on both the a-c and d-c sides. Some name for this type of rectifier is needed.

The effect of commutating reactance in causing d-c voltage regulation is a source of much confusion. It is to be hoped that an exact definition of commutating reactance may help to dispel some of this confusion.

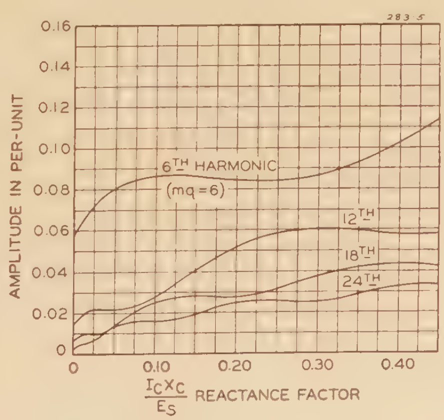

Figure 5. Amplitude of harmonic voltages in d-c voltage wave—characteristic curves. $q=6$

$\alpha=0$

$m=1, 2, 3,$ and 4

Per-unit base is E_{do}

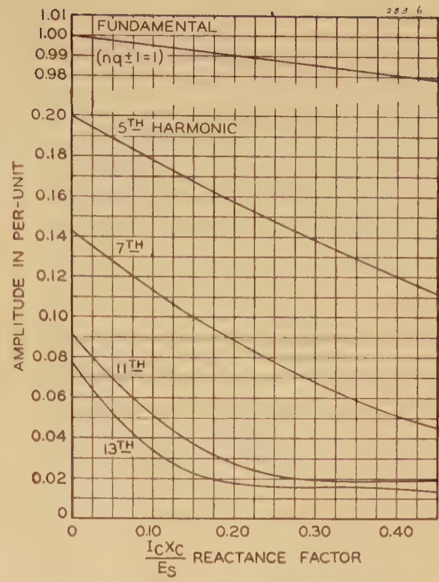

Figure 6. Amplitude of harmonic currents in a-c line current—characteristic curves. $q=6$

$\alpha=0$

$n=0, 1, 2$

Per-unit base is amplitude of fundamental with no overlap

Reactance Factor

The methods employed in analyzing rectifier circuits and the formulas for rectifier-circuit characteristics have been quite thoroughly covered in the literature and are now well understood. However, no standard procedure has been devised for making the circuit calculations or presenting the calculated data, and as a result

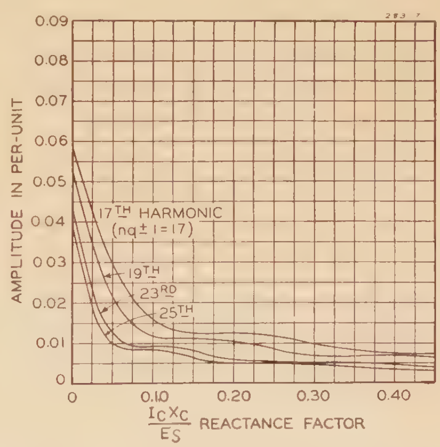

Figure 7. Amplitude of harmonic currents in a-c line current—characteristic curves. $q=6$

$\alpha=0$

$n=3$ and 4

Per-unit base amplitude of fundamental with no overlap

the routine calculation of rectifier characteristics has been more laborious than necessary. Furthermore, with the lack of a standard method for presenting the calculated results, it is difficult to make comparisons between the different types of circuits.

Some attempts have been made to express the rectifier characteristics as a function of a single-circuit factor.[2-5] This work has indicated the usefulness of a factor which combines the current, reactance, and voltage constants of the circuit in a single quantity, although the use of such a quantity has been limited largely to the determination of the harmonic content of the d-c voltage and the a-c line current waves.

This paper proposes the definition and advocates the general use of a single factor of this kind for the purpose of simplifying the description and calculation of the operating conditions of any rectifier circuit. It is further proposed that this factor be known as the "reactance factor" and that it be the product of the direct current commutated I_C and the line-to-neutral commutating reactance X_C divided by the rms value of the transformer secondary line-to-neutral voltage E_S, that is, I_CX_C/E_S.

Circuit Analysis

The rectifier-circuit characteristics depend to a large degree upon the effect of reactance in causing an overlapping of the anode current waves The amount of overlapping is given by the well-known equation for the angle of overlap, which follows:

$$\cos u = 1 - \frac{I_CX_C}{\sqrt{2}E_S \sin \frac{\pi}{P}}$$

With phase control, the overlap equation is

$$\cos(u+\alpha) = \cos\alpha - \frac{I_CX_C}{\sqrt{2}E_S \sin \frac{\pi}{P}}$$

It will be noted that the angle of overlap is a function of the "reactance factor," I_CX_C/E_S. Therefore, for any given value of the reactance factor, the angle of overlap will always be the same, regardless of the particular current, voltage, or reactance constants of the circuit. From this, it follows that if the magnitudes of the current and voltage waves in the rectifier circuit are expressed in per-unit quantities upon a suitable base, the configuration of the current and voltage waves will be the same for any given value of the reactance factor, regardless of the voltage, current, and reactance constants of the circuit. Inasmuch as the reactance factor enters directly in the determination of the overlap, and may be calculated readily from the circuit constants, it provides a convenient quantity for indicating the functional relations between the rectifier characteristics and the circuit constants.

The use of per-unit quantities greatly facilitates the calculation of rectifier circuits. One set of characteristic curves may be prepared for each type of rectifier circuit, if the circuit constants are represented by the reactance factor I_CX_C/E_S and the rectifier characteristics, such as voltage drop, harmonic voltages, and currents, interphase voltages, power factor, and so forth are expressed in per unit. Using such a procedure, it is possible to reduce rectifier calculation to a minimum, calculating the characteristic curves only once. For example, to obtain information regarding any specific circuit, its reactance factor is first calculated from the circuit constants. Reference is then made to the characteristic curves, and the corresponding per-unit value of the desired characteristic determined. The actual numerical value may be calculated by multiplying this per-unit value by the base value. The theoretical d-c voltage E_{do} and the rms value of the transformer secondary line-to-neutral voltage E_S are convenient bases for calculation of per-unit voltages, while the direct current commutated I_C and the theoretical a-c line current I_L are convenient bases for per-unit currents.

The list of rectifier formulas and the accompanying curves show a few of the more commonly used rectifier characteristics, with their equations reduced to their simplest form in terms of reactance factor and the calculated values given on a per-unit basis. These curves illustrate the application of the reactance factor to one type of circuit. Characteristic curves for other types of circuits may readily be prepared from the equations.

Rectifier Formulas

1. Angle of overlap u (Figure 2)

$$\cos(u+\alpha) = \cos\alpha - \frac{1}{\left(\sqrt{2}\sin\frac{\pi}{P}\right)} \cdot \frac{I_CX_C}{E_S}$$

(with no phase control)

$$\cos u = 1 - \frac{1}{\left(\sqrt{2}\sin\frac{\pi}{P}\right)} \cdot \frac{I_CX_C}{E_S}$$

2. Overlap factor $\sqrt{1-cf(u,\alpha)}$ (Figure 3)

where

$$f(u,\alpha) = \frac{1}{2\pi}\left[\frac{\sin u(2+\cos(u+2\alpha)) - u(1+2\cos\alpha\cos(u+\alpha))}{(\cos\alpha - \cos(u+\alpha))^2}\right]$$

3. D-c voltage drop E_X

$$E_X = \frac{I_CX_CP}{2\pi}$$

(in per unit on base E_{do})

$$\frac{E_X}{E_{do}} = \frac{1}{2\sqrt{2}\sin\frac{\pi}{P}} \cdot \frac{I_CX_C}{E_S}$$

4. Power factor PF (Figure 4)

$$PF = K \cdot \frac{\left(\cos\alpha - \frac{E_X}{E_{do}}\right)}{\sqrt{1-cf(u,\alpha)}}$$

5. Harmonics in d-c voltage (Figure 5)

Amplitude of sine components

$$a_m = \frac{E_{do}}{2}\cos m\pi \times \left[\frac{\sin(mq+1)(u+\alpha)+\sin(mq+1)\alpha}{mq+1} - \frac{\sin(mq-1)(u+\alpha)+\sin(mq-1)\alpha}{mq-1}\right]$$

Amplitude of cosine components

$$b_m = \frac{E_{do}}{2}\cos m\pi \times \left[\frac{\cos(mq+1)(u+\alpha)+\cos(mq+1)\alpha}{mq+1} - \frac{\cos(mq-1)(u+\alpha)+\cos(mq-1)\alpha}{mq-1}\right]$$

Amplitude of resultant

$$h_m = \sqrt{a_m^2 + b_m^2}$$

All amplitudes may be expressed in per-unit values by dividing the above equations by E_{do}.

6. Harmonics in a-c line current (Figures 6 and 7)

Amplitude of sine component of fundamental

$$a_1 = \frac{\sqrt{3}I}{\pi}[\cos\alpha + \cos(u+\alpha)]$$

Amplitude of cosine component of fundamental

$$b_1 = \frac{\sqrt{3}I}{\pi}\left[\frac{{}^1/_2\sin(2u+2\alpha) - {}^1/_2\sin 2\alpha - u}{\cos\alpha - \cos(u+\alpha)}\right]$$

Amplitude of the resultant of the fundamental components

$$h_1 = \sqrt{a_1^2 + b_1^2}$$

Temperature-Aging Tests on Class-A-Insulated Fractional-Horsepower Motor Stators

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Synopsis: Temperature-aging data are presented on class-*A*-insulated fractional-horsepower motor stators, determined at 200, 160, and 135 degrees centigrade. Of particular interest is the relative aging time to failure at the different temperatures and the resultant temperature-aging curve. This curve shows a slope of 10 to 15 degrees centigrade increase in temperature to halve the aging time to failure in the temperature range covered, 135 to 200 degrees centigrade.

THE question of the rate at which motor insulation ages due to the temperature to which it is exposed was brought actively to the fore several years ago[1–7] by discussion concerning methods of rating machines whose normal load cycle was intermittent. Many such are encountered in air conditioning, welding, and other applications governed by automatic controls. In such intermittent operation the temperature of the motor varies, depending upon the load cycle to which it is subjected. It is desired that the effect of such temperature variation be not greater, as regards aging of the insulation, than the continuous temperature for which the motor was designed. In order to know this, it is necessary to have a knowledge of the rate of aging at different temperatures. This is really a demand for a knowledge of the aging temperature characteristic of insulation such as is shown in Figure 6.

To obtain such a temperature-aging characteristic, we must carry out our tests at more elevated temperatures, starting perhaps much higher than those in which we are interested, and, through simultaneously started tests at a number of temperatures between this and our operating range, gradually accumulate the data from which our life-temperature curve may be drawn as far down toward the operating range as the results lead us to continue.

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Method of Test

The question of type of test must next be answered. The actual operation of motors of even moderate size, under load in numbers sufficient to yield statistically reliable results, becomes costly and subject to a number of causes of motor failure, such as bearings, starting switches, and so forth.

If we wish to study the influence of temperature on the insulation alone, it is desirable that the tests be conducted so that they are not complicated by these additional sources of failure which arise in an actual running test.

In the past[1] this has been attempted by constructing samples of a variety of kinds, aging them at a number of selected test temperatures, and periodically making a number of tests and measurements of properties, usually destructive. The study of these results enabled one to plot the deterioration of these arbitrarily selected properties in terms of time and temperature, and, with some approximation, to arrive at a temperature-aging relation. One of the present authors gave the results of such a study several years ago.[1] Such tests involve doing things to the sample of insulation such as breaking it, bending it, or otherwise maltreating it. Since service does not involve these factors in similar kind or degree, one usually finished his labors with a curiosity as to

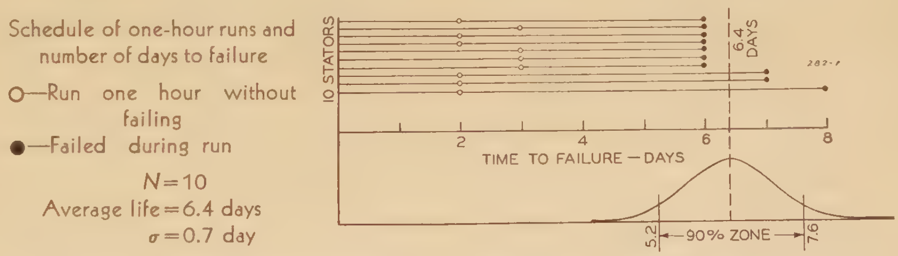

Figure 1. 200 degrees centigrade aging tests on class-*A*-insulated stators, type *N*

Amplitude of sine component of higher harmonics

$$a_m=\frac{4I}{\pi}\cdot\frac{\sin(m\pi/3)}{m(m^2-1)(\cos\alpha-\cos(u+\alpha))}\times$$
$$[\cos\alpha\cos m\alpha-\cos(u+\alpha)\cos m(u+\alpha)+m\sin\alpha\sin m\alpha-m\sin(u+\alpha)\sin m(u+\alpha)]$$

Amplitude of cosine component of higher harmonics

$$b_m=\frac{4I}{\pi}\cdot\frac{\sin(m\pi/3)}{m(m^2-1)(\cos\alpha-\cos(u+\alpha))}\times$$
$$[\cos\alpha\sin m\alpha-\cos(u+\alpha)\sin m(u+\alpha)-m\sin\alpha\cos m\alpha+m\sin(u+\alpha)\cos m(u+\alpha)]$$

Amplitude of the resultant for each of the higher harmonics

$$h_m=\sqrt{a_m^2+b_m^2}$$

The amplitude of the fundamental of the theoretical line current wave, that is, with no phase control ($\alpha=0$) and no overlap ($u=0$) is

$$a_1=\frac{2\sqrt{3}I}{\pi}\qquad b_1=0$$

All amplitudes may be expressed in per-unit values using the amplitude of the fundamental with no phase control and no overlap as a base by dividing the above equations by $\frac{2\sqrt{3}I}{\pi}$.

Refer to references 2, 3, and 5 for symbols not defined in text.

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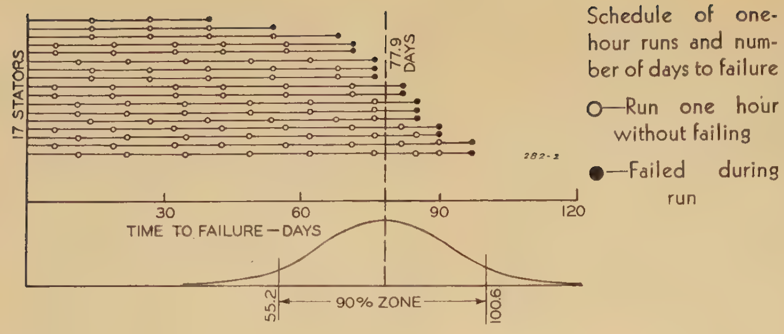


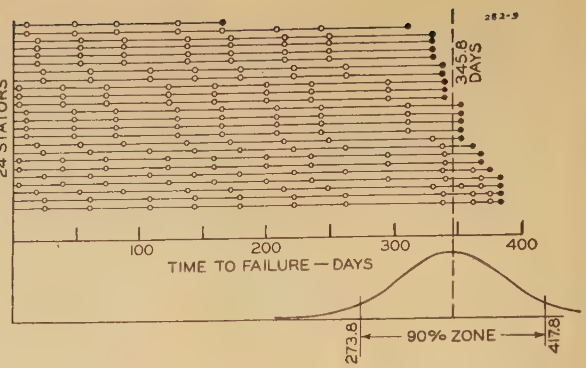


Figure 2. 160 degrees centigrade aging tests on class-*A*-insulated stators, type *N*

$N=17$ Average life = 77.9 days
$\sigma=13.8$ days

what the actual service life at the test temperature might have been.

The present tests followed a course intermediate between the two alternatives outlined above, with the full recognition that the results could not be directly used to predict motor life in service, but hopeful that some light might be thrown on the temperature-aging relationship, even though only in a proportional manner. Recognizing that the completely wound and insulated stators of fractional-horsepower induction motors are both low cost and convenient specimens, and ones which represent a very widely used kind of class-*A* insulation, it was decided to use these for the test specimens. Exploratory tests and previous experience led us to choose aging temperatures of 200, 160, 135, and 115 degrees centigrade. To determine when these stators had temperature-aged to a nonserviceable condition, selected groups were removed from the aging ovens periodically and in turn assembled with the other parts of the motor (squirrel-cage rotor, end shields, bearings, and suitable switch arrangement), and subjected to an actual and severe operating test. This test consisted of a one-hour period of operation at no load, and approximately full-load heating, with a sudden and complete reversal in rotation from full speed every two minutes. On successful completion of this operation test the stators were restored to the aging oven for a continuation of the aging test. When the aging had proceeded to the point at which failures began to be experienced, tests were made more often. Proceeding in this way, an aging time was determined which reduced the insulation to such a state of embrittlement and crumbliness that physical separation was no longer afforded the conductors, and short circuits, or grounds, resulted. This aging time we call the time to failure of the insulation at that temperature for the specific conditions of the test.

Figure 4. 200 degrees centigrade aging tests on class-*A*-insulated stators, type *M1*

$N=10$ Average life = 10.8 days
$\sigma=2.1$ days

Results

Figures 1, 2, and 3 show the results which have been obtained to date at 200, 160, and 135 degrees centigrade for one type of class-*A*-insulated stator, which we will, for purposes of identification, call *N*. Figures 4 and 5 show the results at 200 and 160 degrees centigrade on another type of class-*A*-insulated stator, which we will designate *M*1. Figures 8 and 9 show the results at 200 and 160 degrees centigrade on another type of class-*A*-insulated stator, designated *M*2. Stators *M*1 and *M*2 are similar except for winding differences.

These figures are similarly drawn and show:

1. The time for each stator at which test runs were made.

2. The time of the test run at which failure occurred; this is called the "time to failure" at that stator.

3. The average time to failure of the group of stators tested at each temperature.

Figure 3. 135 degrees centigrade aging tests on class-*A*-insulated stators, type *N*

$N=24$ Average life = 345.8 days
$\sigma=43.7$

4. The root-mean-square deviation, σ, of the time to failure of the individual stators from that of the group average. This is the customary statistical measure of the dispersion of test results.

5. A normal distribution curve calculated from the root-mean-square deviation, σ.

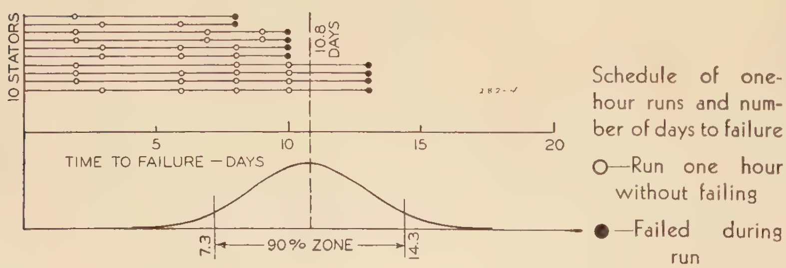
6. The width of the 90 per cent zone or the time interval during which approximately 90 per cent of all the motors fail. This total width is 3.29 σ.

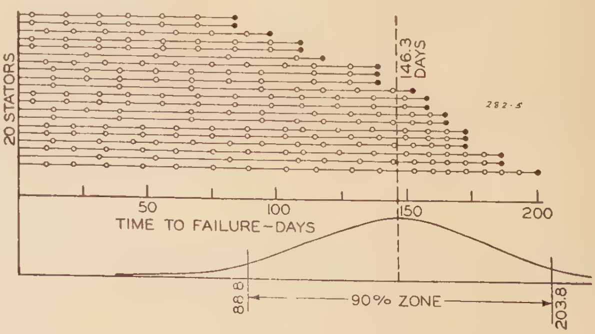
We have purposely placed emphasis in this presentation on the normal variation in the life span, here at least 2 to 1. Life is inherently a variable thing, and engineers must accept this in their motors as they do in their own lives.

Having determined the "time to failure" of these three types of stators at several temperatures, the data can now be plotted in the form of the temperature-aging characteristic listed at the start as the object of our investigation. The average times to failure at the several test temperatures are plotted opposite each temperature. Figure 6 is such a curve for the type *N* stators from 200 down to 135 degrees centigrade (with another point at 115 degrees centigrade due to come at some time in the future). Figure 7 is for the type *M*1 stators, covering only the two temperatures—200 and 160 degrees

Figure 5. 160 degrees centigrade aging tests on class-*A*-insulated stators, type *M1*

$N=20$ Average life = 146.3 days
$\sigma=35.0$ days





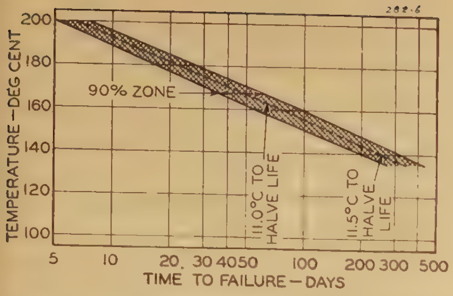


Figure 6. Temperature-aging characteristic, class-*A*-insulated stators, Type *N*

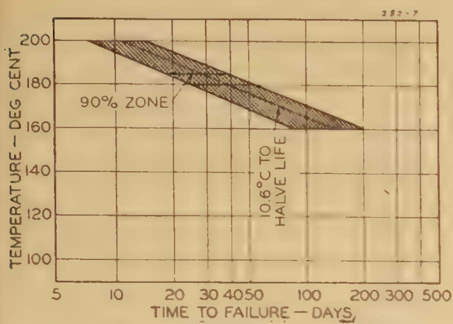


Figure 7. Temperature-aging characteristic, class-*A*-insulated stators, type *M*1

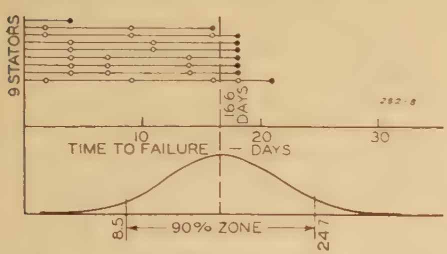


Figure 8. 200 degrees centigrade aging tests on class-*A*-insulated stators, type *M*2

Schedule of one-hour runs and number of days to failure

O—Run one hour without failing

●—Failed during run

N=9 Average life=16.6 days

σ=4.9 days

centigrade, and similarly Figure 10 for the type *M*2 stator.

At the present time after 660 days elapsed time since starting, the tests on the *M*1 stators at 135 degrees centigrade have yielded five failures in 30 stators, 17 per cent. The 135 degrees centigrade point may thus be seen as lining up fairly well with the trend outlined by the two higher temperatures.

Conclusions

A method of studying the temperature-aging characteristics of class-*A*-insulated fractional-horsepower motor stators has been presented. The results available to date cover the tests made at 200 and 160 degrees centigrade and some results at 135 degrees centigrade. The tests are being continued at lower temperatures so that this paper is in the nature of a progress report. The following general statements can be made as to the comparative effects of various temperatures.

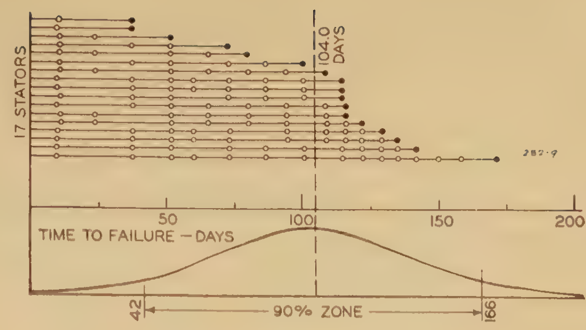


Figure 9. 160 degrees centigrade aging tests on class-*A*-insulated stators, type *M*2

Schedule of one-hour runs and number of days to failure

O—Run one hour without failing

●—Failed during run

N= 17

Average life=104.0 days

σ= 37.5 days

1. Below 200 degrees centigrade there is no temperature which, if exceeded, causes an abrupt failure of the insulation; that is, the temperature-aging curve of the insulation of these motors is uniform and continuous.

2. Operation at higher temperatures than normal results in a shortening of the time to failure of the insulation. Figures 6 and 7 agree that a temperature increment of 10 or 11 degrees centigrade is required to cut the time to failure of the stators in half. This agrees nicely (without the implications of accuracy) with the eight-degree value widely used for transformer insulations.[8,9] Figure 8 gives a corresponding temperature increment of 15 degrees centigrade. The value has not yet been established below the 135 degrees centigrade range in these tests.

3. The width of the "90 per cent zone" (or zone containing 90 per cent of the values) is shown to emphasize the normal spread of results. Its width is obtained from Figures 1 through 5.

4. The normal time to failure of the insulation of duplicate specimens has a spread of 2 to 1.

It will be noted that no conclusion as to the probable actual operating life of the insulation for any given temperature has been drawn from these tests. This is for the reason that, as pointed out previously, the tests have been made in such a manner as to single out the effects due to temperature and to exclude other effects which would be met with in actual operation of the motor in service. This might include the effect of moisture, the effect of dirt, oil, or various gases in the atmosphere, the effect of vibration which might

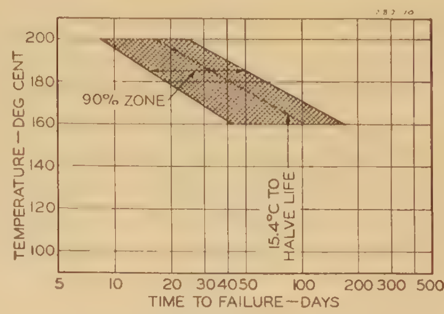


Figure 10. Temperature-aging characteristic, class-*A*-insulated stators, type *M*2

be encountered with various types of loads, changes in ambient temperature, intermittent loading, and other factors, some of which may tend to increase and others to decrease the life in actual operation.

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Theory of the Brush-Shifting A-C Motor—III

Power-Factor Correction

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Synopsis: In the preceding papers[1] of this series a method was described for determining the circle diagram and predicting the characteristics for the brush-shifting motor as it is used when the brushes are shifted to control speed. This paper extends the earlier work to show how the motor can be used to correct power factor. The range and limitations for this condition of operation are revealed from the circle diagram of the motor which has been developed and verified experimentally. Characteristics of the motor when used in this manner are presented.

Brush Settings for Power-Factor Correction

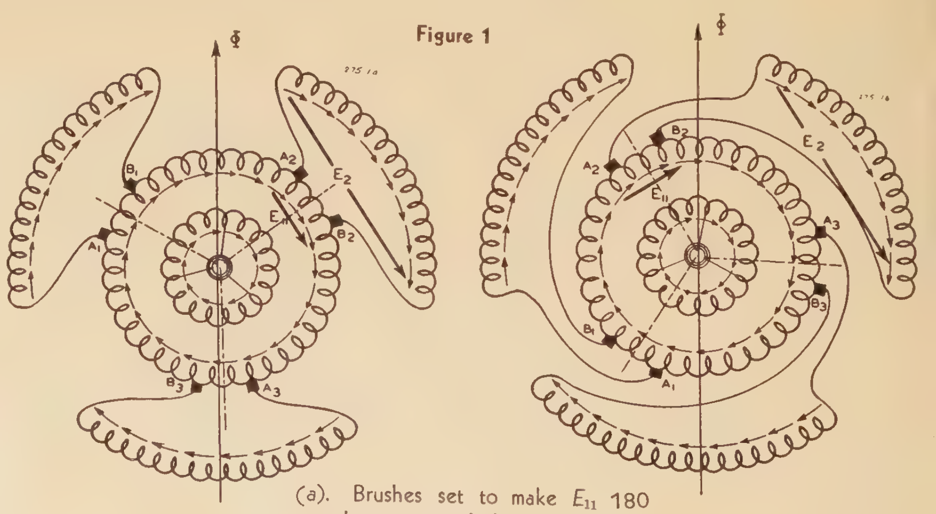
UNDER normal operation of the brush-shifting a-c motor, the two sets of brushes which carry the secondary current of the stator coils are coupled mechanically so that one set of brushes cannot be moved without a corresponding motion of the other in the opposite direction. Thus, the voltage introduced in the secondary circuit from the commutator, while it may vary in magnitude with speed adjustment, does not vary in phase relative to the voltage induced in the stator winding. Figure 1a is a schematic diagram of a two-pole, three-phase brush-shifting motor. The horizontal projections of the vectors represent the instantaneous voltages induced in the adjacent coils. The flux, ϕ, rotates at slip speed, say clockwise, relative to the stator. Brushes A_1, A_2, and A_3 are mounted rigidly on an adjustable frame so that a motion of the frame will move them all through the same angle; brushes B_1, B_2, and B_3 are mounted similarly and also move in unison. These two frames are coupled mechanically so that when making speed adjustments, the brushes are always equal distances from the center lines drawn between them. The points of maximum and minimum potential about the commutator rotate with the field relative to the stator so that the voltages at the brush pairs are at slip frequency. If the center lines drawn midway between the brushes A_1 and B_1, A_2 and B_2, and so on, remain fixed relative to the stator, the voltages collected from the commutator are fixed in phase relative to the induced voltage in the stator. The magnitude of this voltage, E_{11}, will vary with the degree of separation of the brushes but is approximately independent of speed for a given primary voltage. Interchanging the positions of the brushes A_1 to B_1, B_1 to A_1, and so on, will change the phase by 180 degrees, that is, reverse the polarity of the commutator voltage, E_{11}.

If the coupling between the frames supporting each set of brushes is removed, either set can be moved in either direction independently of the other. The various possible brush settings provide the motor with an extremely wide range of characteristics. If the flux, ϕ, is rotating clockwise relative to the stator, and the two frames are moved opposite to the direction of rotation of the flux with respect to the stator by 90 degrees from the position shown in Figure 1a (see Figure 1b), the voltage, E_{11}, will be advanced by 90 degrees inphase with respect to the voltage, E_2, induced in the stator. This procedure will advance the center lines designating the brush positions by 90 degrees relative to the stator. The phase of the voltage, E_{11}, collected from the commutator can be varied only by altering the mean position of the brushes as indicated by these center lines.

Figure 2a shows the voltage, E_2, induced in one phase of the secondary when brushes are set as in Figure 1a, that is, when E_2 is in opposition to E_{11}. Figure 2b shows what occurs when both sets of brushes are rotated in a direction so as to advance E_{11} by 90 degrees as illustrated in Figure 1b. Since both voltages, E_2 and E_{11}, are in series, and force currents through the same impedance, I_2 and I_{11} of Figure 2b must lag their voltages by the same angle. I_2 and I_{11} are the currents flowing in the secondary resulting from the voltages E_2 and E_{11} respectively. The two currents and their circle loci are shown. If the center lines are allowed to remain in their new positions of Figure 1b, and the two sets of brushes are moved across the center lines, that is, brushes A and B interchange positions, the voltage, E_{11}, from the adjusting winding will be shifted in phase by 180 degrees from the direction shown in Figure 2b, and E_{11} will lead E_2 by 90 degrees as shown in Figure 2c. The current I_2 lags the voltage E_2 by the same angle that the current I_{11} lags the voltage E_{11}.

If the motor is designed so that the effective number of series turns in the adjusting winding is half the effective number of stator turns, the largest value of the voltage, E_{11}, is half the stator voltage, E_2 at standstill. If in Figures 2a, 2b, and 2c, the brushes of each phase are spaced 180 electrical degrees apart on the commutator, E_{11} will have its largest

Figure 1



(a). Brushes set to make E_{11} 180 degrees out of phase with E_2
(b). Brushes shifted to make E_{11} lead E_2 by 90 degrees

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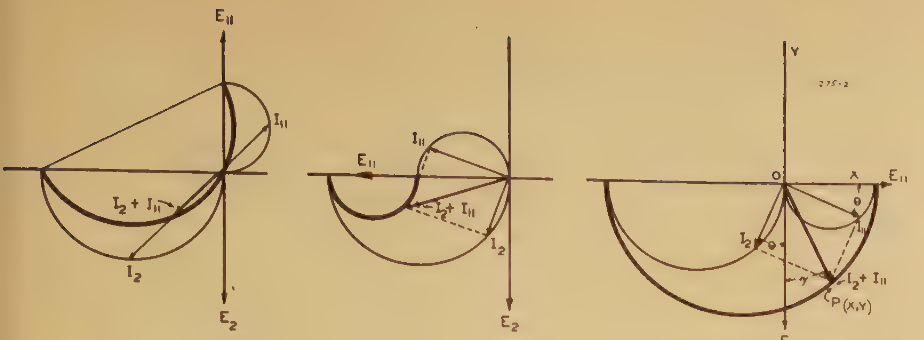


(a) (b) (c)

(*a*). E_{11} 180 degrees out of phase with E_2 (*b*). E_{11} lagging E_2 by 90 degrees (*c*). E_{11} leading E_2 by 90 degrees

Figure 2. Vector diagrams of secondary voltages and currents when brushes are set to make

value. The largest value of I_{11} (its value at synchronous speed) and the largest value of I_2 are as determined in part I of this series. The resultant secondary current is the vector sum of I_2 and I_{11}, and, as is shown in Figures 2b and 2c, this current is never zero whenever E_{11} has a quadrature component relative to E_2. The locus of the extremity of the vector representing the sum of I_2 and I_{11} is derived in appendix A, and is found to be another circle shown in wide lines in Figures 2b and 2c.

In Figure 2b it is evident that the total current flowing in the secondary lags the voltage, E_2, induced in the secondary by a relatively large angle at all times. In Figure 2c the total secondary current leads the voltage, E_2, over a considerable portion of the circle. These features enable the machine to generate power at a lagging power factor when used as a generator excited from a synchronous source. Furthermore it can supply these lagging loads over a wide range of speeds. This paper is primarily concerned with power-factor correction, and, therefore, will deal only with conditions illustrated in Figure 2c, rather than those of Figure 2b.

Primary Currents With Power-Factor Correction

The phase current in the primary of this motor can be determined from a knowledge of the magnetomotive forces in the adjusting winding and the stator winding for different values of slip. For each current I_2 and I_{11}, shown in Figure 2c, there is a corresponding magnetomotive force produced in each of these windings. The magnetomotive force produced by the current I_{11}, of Figure 2c, in the stator is represented by the vector *oa* of Figure 3. Likewise, the magnetomotive force produced by the current I_2 of Figure 2c in the stator is represented by the vector *ob* of Figure 3. The total magnetomotive force produced in the stator by the currents I_2 and I_{11} is therefore the vector *oc* of Figure 3, which is the sum of *oa* and *ob*. The locus of this stator magnetomotive force, *oc*, is defined by the circle *dce*. With this particular brush setting on a three-phase motor, there is a magnetomotive force produced by these same currents, I_2 and I_{11}, flowing in the adjusting windings. This magnetomotive force is 90 degrees behind the magnetomotive force that these currents produce by flowing in the stator. The vector oc' therefore represents the adjusting winding magnetomotive force when the stator magnetomotive force is *oc*. The locus of the vector oc' is the circle $d'c'e'$. The proportions of this circle are such that

$$\frac{oc'}{oc}=\frac{od'}{od}=\frac{oe'}{oe}=\frac{N_{AW}}{N_2}$$

With the brushes set as described here, motor action results only at speeds below synchronism. For such speeds, magnetomotive forces in the stator and adjusting windings are cancelled by equal and opposite magnetomotive forces in the primary. In Figure 3, the stator magnetomotive force *co* is counteracted by the component primary magnetomotive force oc'', and the adjusting winding magnetomotive force oc' is counteracted by the component primary magnetomotive force oc'''. The total magnetomotive force of the primary that is necessary to counteract the magnetomotive forces of the adjusting winding and the stator is the sum of oc'' and oc''', which is oc''''. The locus of the extremity of this vector oc'''' is defined by the circle $d''''c''''e''''$. The primary magnetomotive force must be produced by a current which is proportional to and in phase with oc''''. In Figure 3 this current is shown as the vector I_{1L}. The locus of I_{1L} is another circle having a center coincident with the center of the circle $d''''c''''e''''$. The locus of the primary current can be obtained by adding to this current I_{1L}, the current *po*. The current *po* is the no-load current taken by the motor when the brushes are set to make

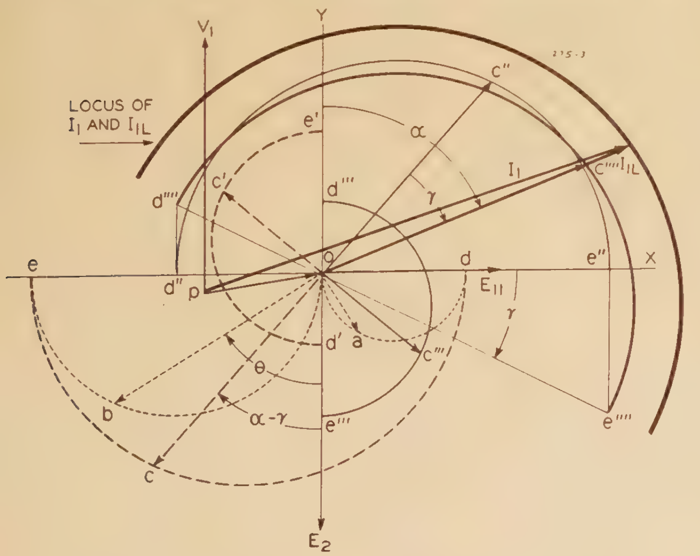


Figure 3. Vector diagram of motor when used to correct power factor

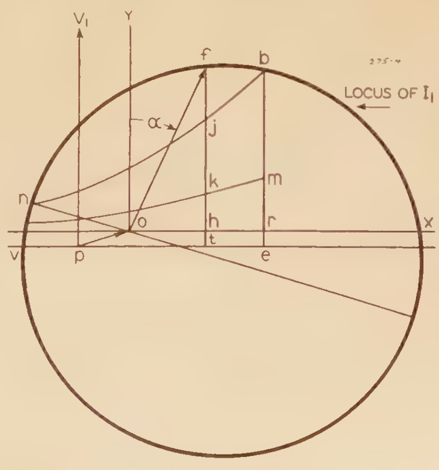


Figure 4. Characteristic vector diagram of motor (primary), E_{11} 90 degrees ahead of E_2

E_{11} zero. The total current taken by the primary, shown on this diagram, is the vector I_1, and the locus of its extremity with respect to the point p is defined by the same circle that defines I_{1L}. This circle has a diameter passing through the point o at an angle γ, the tangent of which is equal to the ratio of the effective number of turns in the adjusting winding between brushes to the effective number of stator turns, N_{AW}/N_2.

A change in the brush setting will change the total resistance and reactance of the total secondary circuit and, consequently, change the diameters of both the circle loci of I_{11} and I_2. With the brushes set to obtain the characteristics illustrated in Figure 3, the motor will take a leading current, I_1, with respect to the impressed voltage, V_1 over a considerable range of loads. For such conditions of load, it can be used to correct the power factor of the line supplying the motor.

Determination of Characteristics When Motor Is Used to Correct Power Factor

On the basis of the theory and assumptions of part I of this series, it was shown that the currents I_2 and I_{11} could be expressed thus:

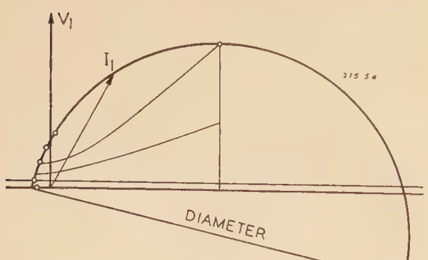
$$I_2=\frac{{}_{SS}E_2S}{\sqrt{(R_2+R_{AW})^2+S^2({}_{SS}X_2+{}_{SS}X_{AW})^2}} \qquad (1)$$

$$I_{11}=\frac{E_{11}}{\sqrt{(R_2+R_{AW})^2+S^2({}_{SS}X_2+{}_{SS}X_{AW})^2}} \qquad (2)$$

Therefore

$$\frac{I_2}{I_{11}}=\frac{{}_{SS}E_2S}{E_{11}} \qquad (3)$$

If the brushes supplying each stator phase



(a)

Figure 5

(a). Circle diagram obtained from no-load tests. Encircled points indicate primary currents taken by motor under load conditions (determined by loading)

(b) and (c). o---o---o Characteristics obtained by loading

———— Characteristics predicted from the circle diagram

are set so that E_{11} equals half ${}_{SS}E_2$, as is illustrated in Figures 2b and 2c, then

$$\frac{{}_{SS}E_2S}{E_{11}}=2S \qquad (4)$$

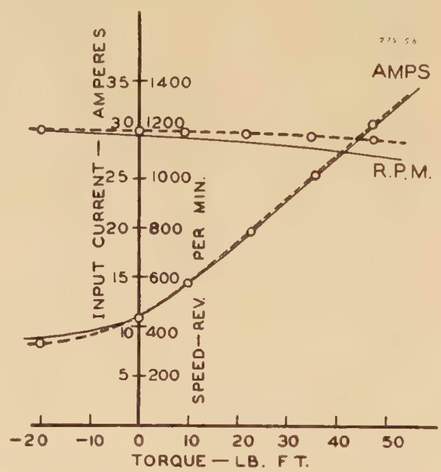
At standstill S equals one, and

$$\frac{I_2}{I_{11}}=2 \qquad (5)$$

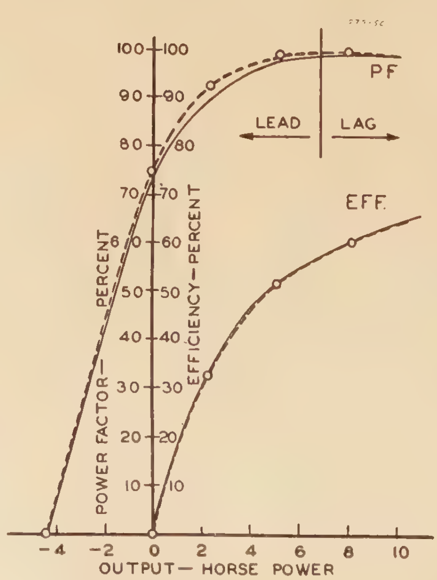
Since the ratio of $I_{2\max}$ to $I_{11\max}$ is, from equations 1 and 2,

$$\frac{I_{2\max}}{I_{11\max}}=\frac{{}_{SS}E_2}{E_{11}}\left(\frac{R_2+R_{AW}}{{}_{SS}X_2+{}_{SS}X_{AW}}\right) \qquad (6)$$

a knowledge of the ratio of R_2+R_{AW} to ${}_{SS}X_2+{}_{SS}X_{AW}$ is all that is required to determine the diameters of the two component circles, I_2 and I_{11}, and from them the resultant circle defining the extremity of the primary current vector as described above. At synchronous speed I_2 is zero and I_{11} is maximum. If the primary currents for standstill and for some other speed are known, it is possible to determine the characteristic



(b)



(c)

copper loss curves for the circle diagrams as it is done for the two-element induction motor. At standstill all the energy in excess of the no-load losses is lost in the primary and secondary windings, while for other speeds these losses vary as the square of that component of the primary current which is labelled I_{1L} in Figure 3. This involves the same approximation with regard to losses that is made in plain induction motor theory; namely, that the primary copper loss varies as the square of the load component of primary current rather than as the total primary current. The error introduced by this approximation in the two-element induction motor is negligible. The error due to this approximation is somewhat larger in this motor than in the two-element motor, but not so large as to invalidate the method.

The characteristic copper loss curve, bjn, is shown in Figure 4. The point, m, determined by measurement of primary resistance divides rb into components proportional to the primary and secondary losses. The current pb produces a primary loss which is represented by mr. The loss for any primary current, pf, can be represented by kh which in turn can be determined by the relationship

$$kh=mr\left(\frac{pf}{pb}\right)^2$$

The secondary copper loss represented by jk can be found in similar fashion.

The slip is a function of the angle $(\alpha-\gamma)$ by which I_2+I_{11} lags E_2. This is also the angle between the vector oc in Figure 3, representing the magnetomotive force due to I_2+I_{11} in the stator windings, and the vector labelled E_2, the voltage induced in the stator by the air-gap flux. The angle α is the phase angle between the voltage V_1 and the primary-current component I_{1L}, or the magnetomotive force oc''''. This angle varies with slip. The angle γ is determined by its tangent which equals N_{AW}/N_2; thus, γ is independent of slip. The relation between slip and $(\alpha-\gamma)$ is given by

$$\tan(\alpha-\gamma)=\frac{1-\left(\frac{1}{\tan\gamma}\right)^2\left(\frac{I_{11\max}}{I_{2\max}}\right)S^2}{\left(\frac{1}{\tan\gamma}\right)\left(\frac{I_{11\max}}{I_{2\max}}+1\right)S} \qquad (7)$$

This equation is developed in appendix B and may be used to determine the slip at any point f (see Figure 4) on the circle defining the extremity of I_1. The speed may be obtained, since speed equals synchronous speed times the quantity, $(1-S)$. If the motor is loaded so that the primary draws a phase current pf in Figure 4, the slip will have some value,

S, corresponding to the point, f, and can be determined from measurements of α and equation 7.

If the impressed voltage per phase is represented by V_1, then for the input current, pf:

(1) Input $=(tf)\,V_1$ watts per phase
(2) Output $=(jf)\,V_1$ watts per phase
(3) Iron loss, friction, and windage $=(th)\,V_1$ watts per phase
(4) Primary copper loss $=(hk)\,V_1$ watts per phase
(5) Copper loss of stator and adjusting winding $=(kj)\,V_1$ watts per phase
(6) Efficiency $=\dfrac{jf}{tf}$ times 100 per cent
(7) The slip is determined by the angle between of and V_1 (or between of and oy) using the equation (7)
(8) Speed $=(1-\text{slip})$ (synchronous speed in rpm) in rpm
(9) Torque $=\dfrac{(jf)\,V_1\,(\text{number of phases})\,33{,}000}{2\pi\,(\text{speed in rpm})\,746}$ in pound-feet

The output torque is zero at the point n in Figure 4. For slips less than that at n, power must be supplied to the machine through the shaft. At the point v, slightly above synchronous speed, all the power into the machine comes by way of the shaft.

Results of Test With Leading Commutator Voltage

In order to check the validity of the foregoing theory, a test was made on a General Electric BTA, 550–1,650-rpm. 4.17–12.5-horsepower, six-pole, 60-cycle motor. With the brushes set so that the commutator voltage was ahead of the induced voltage of the stator by 90 degrees, the gap between brushes was fixed so that the commutator voltage was 7.8 per cent of the stator voltage at standstill. The results of the load test and the blocked rotor test are indicated by the encircled points on the circle diagram of Figure 5a. The theoretical primary-current circle locus was determined from three no-load readings as described in parts I and II of this series. It is observed that the diameter of the circle lies beneath the horizontal chord by an angle of approximately ten degrees. Only about five degrees of this shift is accounted for by γ (the shift caused by the magnetomotive force of the adjusting winding). The remainder can be attributed directly to the leakage reactance of the primary circuit which, with high current values, causes the induced voltage, E_2, in the secondary to lag the primary voltage. Figure 5 shows the results predicted theoretically in comparison with the experimentally determined results.

Conclusions

The advantages of power-factor correction on a line supplying a motor are well-known. Advantages of power-factor correction to the motor itself result only when these corrections increase the efficiency of the motor or its horsepower capacity. The effect of power-factor correction on the efficiency of this motor is illustrated in Figure 6. Each of the efficiency curves shown here was obtained by a load test with brush settings that provided synchronous speed at no load.

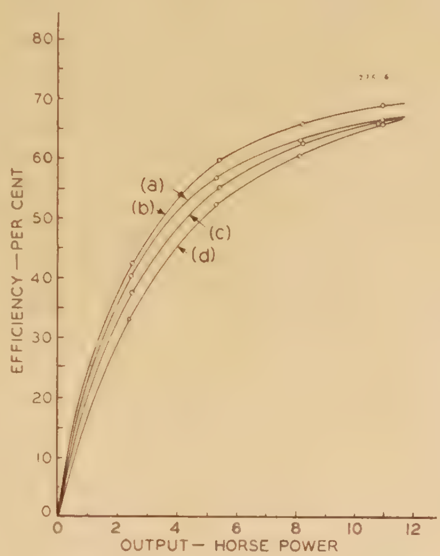


Figure 6. Efficiency as affected by power-factor correction

Brushes separated
(a). 0 electrical degrees
(b). 6 electrical degrees
(c). 12 electrical degrees
(d). 18 electrical degrees

It is evident that the addition of a small value of E_{11} (two per cent of its maximum value, that is, a brush separation of approximately five electrical degrees) to improve power factor will increase the efficiency only slightly. Further increases in E_{11} will cause excessive secondary currents and reduce the efficiency. It is evident from the theory developed here that if the primary exciting current is to be reduced the secondary must carry an additional exciting current to compensate for the reduction in the primary. Maximum efficiency for a given load will occur when the voltage E_{11} is adjusted so as to make the total copper losses in the motor a minimum. Any exciting current flowing in the secondary must be supplied through the commutator. For some conditions of load this additional current will cause poor commutation. Thus there is little to be gained from standpoint of the motor by setting its brushes to improve its power factor beyond the values obtained when E_{11} is made opposite to E_2.

From information revealed in Figure 5 it is quite evident that the motor can be adjusted to draw various amounts of leading current. This feature provides the possibility of improving the regulation and efficiency of the line supplying the motor. These curves of Figure 5 also show the relative high power factor obtainable on the brush-shifting motor as compared with the ordinary well-known two-element induction motor.

Appendix A

It can be shown that the locus of the sum of I_2 and I_{11} is a circle as follows:

The line OP of Figure 2c indicates the current vector representing the sum of I_2 and I_{11}. Let x equal the horizontal component of OP, and y equal the vertical component of OP. Since I_2 and I_{11} are always at right angles

$$OP^2=I_2^2+I_{11}^2$$

or $$OP^2=I_2^2+I_{11}^2 I_{2\max}I_{11\max}-I_{2\max}I_{11\max}(\sin^2\theta+\cos^2\theta)$$

But $I_2=I_{2\max}\sin\theta$

$$I_{11}=I_{11\max}\cos\theta$$

$$OP^2=I_2I_{2\max}\sin\theta+I_{11}I_{11\max}\cos\theta+I_{2\max}I_{11\max}-I_{2\max}I_{11}\cos\theta-I_{11\max}I_2\sin\theta$$

$$OP^2=I_{2\max}(I_2\sin\theta-I_{11}\cos\theta)-I_{11\max}(I_2\sin\theta-I_{11}\cos\theta)+I_{2\max}I_{11\max}$$

$$OP^2=(I_{11\max}-I_{2\max})(I_{11}\cos\theta-I_2\sin\theta)+I_{2\max}I_{11\max}$$

but $x=I_{11}\cos\theta-I_2\sin\theta$

and $OP^2=x^2+y^2$

$$x^2+y^2=(I_{11\max}-I_{2\max})x+I_{2\max}I_{11\max}$$

$$y^2+x^2+x(I_{2\max}-I_{11\max})+\frac{(I_{2\max}-I_{11\max})^2}{4}=I_{2\max}I_{11\max}+\frac{(I_{2\max}-I_{11\max})^2}{4}$$

$$y^2+\left(x+\frac{I_{2\max}-I_{11\max}}{2}\right)^2=\left(\frac{I_{2\max}+I_{11\max}}{2}\right)^2$$

This is the equation of a circle of radius $(I_{2\max}+I_{11\max}/2)$ with its center at $x=-{}^1/_2(I_{2\max}-I_{11\max})$ and $y=0$. A similar method may be used to show that the locus of P in Figure 2b is also a circle.

Appendix B

In order to establish equation 7, it is necessary to refer to Figure 3. In this figure $(\alpha-\gamma)$ is the angle by which oc lags E_2. The vector oc represents the magnetomotive force produced in the stator by I_2+I_{11}. The magnetomotive forces produced in the stator by I_2 and I_{11} separately are ob and oa respectively. The currents I_{11}, I_2, and

I_2+I_{11} with proper change in scale could be represented by *oa*, *ob*, and *oc*. If this is done, and if the point *c* is determined by the rectangular co-ordinates *x* and *y*, then

$$\tan(\alpha-\gamma)=-\frac{x}{y}$$

Since *oe* now represents I_{2max} and *od* represents I_{11max}, if θ is the power factor angle of the secondary circuit:

$$I_2=I_{2max}\sin\theta$$
$$I_{11}=I_{11max}\cos\theta$$

In Figure 3

$$x=I_2\sin\theta-I_{11}\cos\theta$$
$$y=I_2\cos\theta+I_{11}\sin\theta$$

so

$$\tan(\alpha-\gamma)=-\frac{x}{y}=\frac{I_{11}\cos\theta-I_2\sin\theta}{I_{11}\sin\theta+I_2\cos\theta}$$

or

$$\tan(\alpha-\gamma)=\frac{1-\dfrac{I_2}{I_{11}}\tan\theta}{\tan\theta+\dfrac{I_2}{I_{11}}}$$

From above

$$\frac{I_2}{I_{11}}=\frac{I_{2max}}{I_{11max}}\tan\theta$$

and from equation 3,

$$\frac{I_2}{I_{11}}=\frac{{}_{ss}E_2S}{E_{11}}$$

therefore

$$\tan\theta=\frac{I_{11max}}{I_{2max}}\left(\frac{{}_{ss}E_2S}{E_{11}}\right)$$

Since $\dfrac{E_{11}}{{}_{ss}E_2}=\dfrac{N_{AW}}{N_2}=\tan\gamma$, it follows that

$$\tan\theta=\frac{I_{11max}}{I_{2max}}\left(\frac{S}{\tan\gamma}\right)$$

$$\tan(\alpha-\gamma)=\frac{1-\left(\dfrac{1}{\tan\gamma}\right)^2\left(\dfrac{I_{11max}}{I_{2max}}\right)S^2}{\left(\dfrac{1}{\tan\gamma}\right)\left(\dfrac{I_{11max}}{I_{2max}}+1\right)S}$$

Appendix C. List of Symbols

E_{11}—Voltage generated in adjusting winding between brushes supplying one phase of stator

E_2—Voltage induced in one phase of stator

${}_{ss}E_2$—Stator voltage at standstill

ϕ—Total flux produced by primary windings

α—Angle between V_1 and I_{1L}

γ—Angle $\tan^{-1}\dfrac{N_{AW}}{N_2}$

$(\alpha-\gamma)$—Phase angle between total secondary phase current and voltage induced in stator

I_{1L}—Load component of primary current

I_1—Total primary phase current

I_{11}—Current flowing in stator and adjusting winding resulting from the voltage E_{11}

I_2—Current flowing in stator and adjusting winding resulting from the voltage E_2

I_{11max}—Maximum value of I_{11} and thus diameter of I_{11} circle

I_{2max}—Maximum value of I_2 and thus diameter of I_2 circle

N_2—Effective number of stator turns per phase

N_{AW}—Effective number of adjusting-winding turns per phase

R_{AW}—Resistance of adjusting winding per phase

R_2—Resistance of stator per phase

S—Per cent slip

θ—Phase angle between I_{11} and E_{11}, or I_2 and E_2

V_1—Voltage impressed upon one phase of primary

${}_{ss}X_{AW}$—Adjusting-winding reactance at standstill

${}_{ss}X_2$—Stator reactance at standstill

Reference

1. Theory of the Brush-Shifting A-C Motor—I, II, A. G. Conrad, F. Zweig, and J. G. Clarke. AIEE Transactions, volume 60, 1941, August section, pages 829–36.

Theory of the Brush-Shifting A-C Motor—IV

Speed Control With Power-Factor Correction

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Synopsis: The preceding papers of this series presented an analysis of the brush-shifting a-c motor. Parts I and II dealt with the case when the voltage introduced into the stator coils from the commutator was collinear with the voltage induced in the stator coils by slip. Part III dealt with the case when the voltage introduced into the stator coils from the commutator was in quadrature with the induced voltage.

This paper extends the earlier analysis to include all the possible phase positions of the voltage introduced into the stator coils from the commutator. Under these conditions power factor and speed are controlled simultaneously. Methods for constructing the circle diagram for these conditions are given, and the characteristics of the motor predicted from the circle diagram are compared with the characteristics obtained by laboratory tests.

It has been demonstrated[1] that the locus of the extremity of the current vector of the brush-shifting motor is a circle when the brushes are set for speed adjustment, that is, when the voltage, E_{11}, collected from the commutator is collinear with the induced voltage, E_2, in the stator. Further analysis[3] has shown that when the brushes are set to make E_{11} perpendicular to E_2 for the purpose of power-factor correction, that the locus of the extremity of the current vector is also a circle. An analysis of these circles has provided[1,3] a means of explaining the operation of the motor for these special brush settings and of predicting all of its characteristics from no-load measurements.

The developments described above have been made from an analysis of:

(*a*). The secondary currents I_{11} and I_2 produced by the voltages E_{11} and E_2 respectively.

(*b*). The magnetomotive forces produced by these currents flowing in the stator and the adjusting winding.

(*c*). The resultant magnetomotive forces produced in the primary winding as a result of the secondary magnetomotive forces.

(*d*). The primary currents associated with the required primary magnetomotive forces.

This paper, employing the same methods of analysis, extends the theory of the brush-shifting motor and explains its operation when the brushes are shifted to control speed at the same time that the motor is used to correct the power factor of its supply. Specifically, it deals with the operation of the motor when the brushes are set to make E_{11} out of phase with the standstill value of E_2 by any angle β. An understanding of the developments presented here presupposes a knowledge of the material presented in the preceding papers of this series.[1-3]

Figure 1a shows a representative vector diagram of the secondary currents I_{11} and I_2, and their loci when they are produced by the voltages E_{11} and E_2 which are no longer collinear. Appendix A demonstrates that the sum of I_{11} and I_2 for this condition is a vector the locus of which is defined by another circle. This circle, representing the locus of the sum of I_2 and I_{11}, is shown in Figure 1a. For the condition shown, the machine will run above synchronous speed at no load, and the power factor will be leading. The current vectors will follow the circle loci shown when the speed is varied from synchronous speed through higher speeds to infinite speed—infinite negative slip. Since the diameter of the I_{11} circle coincides with E_{11}, swinging E_{11} to various phase positions moves the I_{11} circle along with it. For values of β ranging from 0 to 180 degrees, a current is reflected into the primary which has a leading component over the operating range of the motor. The no-load speed of the machine is above synchronous speed if β is between 0 and 90 degrees, as in Figure 1, and below synchronous speed if β is between 90 and 180 degrees.

When the brushes are set to make E_{11} in the same direction as ${}_{ss}E_2$, ($\beta=0$), the center lines of corresponding stator and adjusting winding coils are collinear, and their magnetomotive forces aid. With this reference position of E_{11}, β is also the angle by which the magnetomotive force of the adjusting winding lags the magnetomotive force of the stator coils. This is the angle in electrical degrees through which it is necessary to rotate the brushes about the commutator in order to shift E_{11} by β degrees (see Figure 2). Only the phase position of E_{11} is changed if the brushes are all rotated about the commutator by the same angle, keeping their relative spacing unchanged. Positive values of β are obtained by moving the brushes in the direction of rotation from the position where $\beta=0$. Values of magnetomotive force and of E_{11} identical to those at any given setting of the brushes can be obtained by rotating all brushes 360 electrical degrees around the commutator, or by interchanging the positions of all brush pairs, and moving them all 180 electrical degrees around the commutator.

Determination of the Primary Currents

The primary current resulting from the secondary currents shown in Figure 1a can be obtained from a consideration of the various magnetomotive forces involved. If the resultant flux crossing the air gap is to remain unchanged (so that the generated voltage will remain approximately equal and opposite to the applied voltage), the magnetomotive forces produced by the secondary currents flowing in the stator and the adjusting winding coils must be cancelled by component magnetomotive forces produced by component currents flowing in the primary coils.

While the polyphase current I_2+I_{11}, which flows in the stator coils also flows in the adjusting winding coils, the magnetomotive forces produced by these two sets of coils in series are not, in general, in the same time phase with respect to the primary. This is because the adjusting winding coils are mechanically displaced from the stator coils by β electrical degrees, as shown in Figure 2. If the brushes have been shifted to retard E_{11} by β degrees (which advances the primary current), the magnetomotive force of the adjusting winding lags the magnetomotive force of the stator by β degrees, so that the primary current cancelling the magnetomotive force of the adjusting winding lags the primary current which cancels the magnetomotive force of the stator by β degrees.

Figure 1b shows a vector diagram of the components of the primary current necessary to cancel the magnetomotive

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force produced by the secondary currents of Figure 1a flowing in the *stator*. Figure 2 shows that this component of primary current is obtained by a *mirror reflection* of the secondary current while the motor is running above synchronous speed, and by a *180-degree reflection* of the secondary current when the machine is running below synchronous speed. Figure 1c shows a vector diagram of the components of primary currents which cancel the magnetomotive forces of the secondary currents flowing in the *adjusting winding*. Figure 1d shows the magnetizing component of the primary current. Figure 1e shows the sum of these component currents, or I_1, the total primary current. At a given load, the currents reflected from the adjusting winding and the stator winding are separated by the angle β. Their sum is separated from the component reflected from the stator by the angle

$$\gamma = \tan^{-1}\frac{N_{AW}\sin\beta}{N_2+N_{AW}\cos\beta}$$

The currents reflected into the primary from the stator and the adjusting winding bear the ratio N_2 to N_{AW}.

By adding the reflected currents vectorially, the load component of the primary current is obtained. The ratio of transformation between the secondary and the primary can be obtained from the equation

$$N_1 I_{1L} = \sqrt{(N_2+N_{AW}\cos\beta)^2+(N_{AW}\sin\beta)^2}\,(I_2+I_{11})$$

Obtaining the Circle Diagram

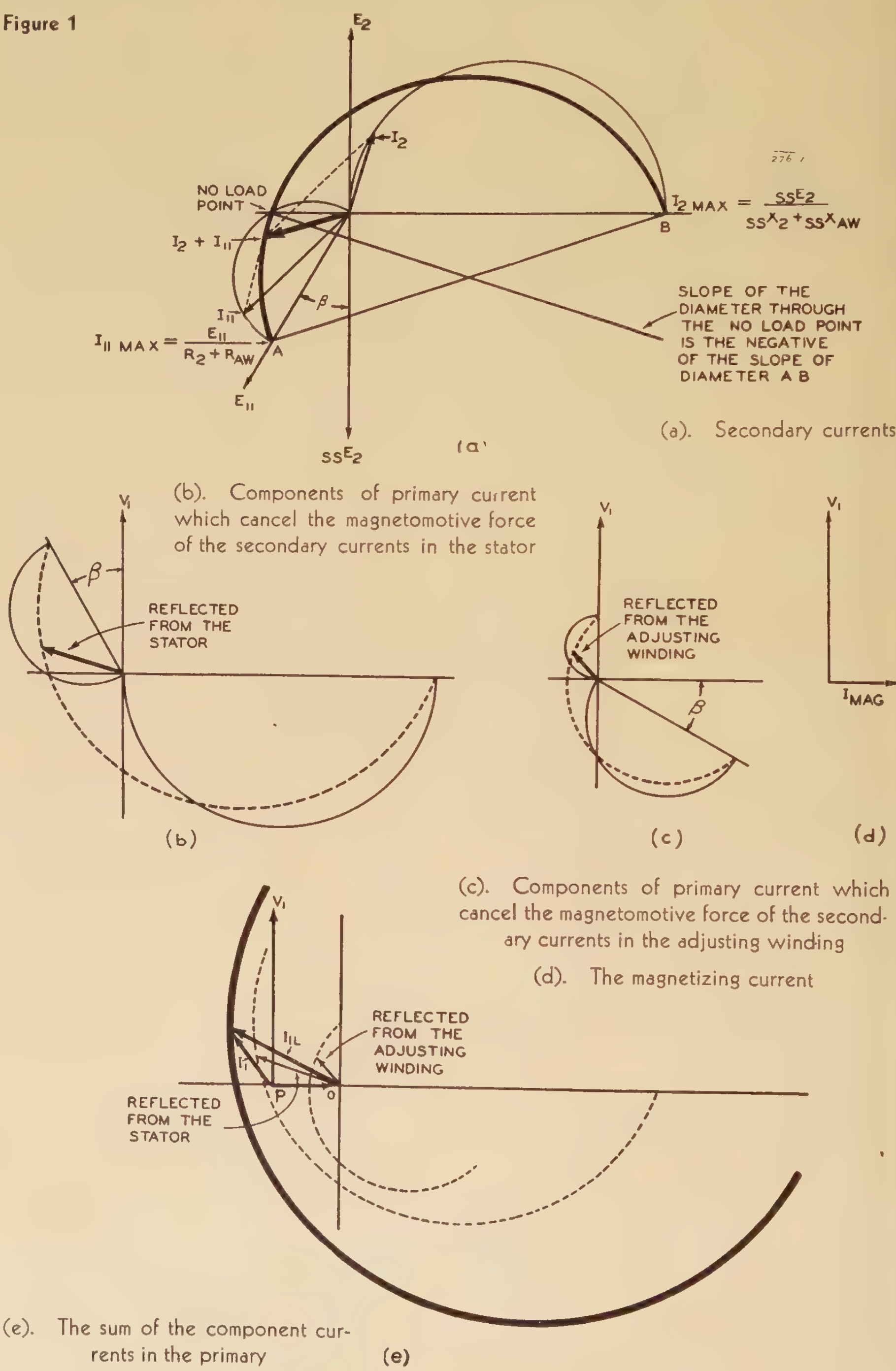
For specific cases, two general methods have been considered for obtaining the circle locus of the primary current without loading the machine. One method makes use of the fact that a circle is uniquely determined if two points and the slope of the diameter through one of the points is known. This method is commonly used to obtain the circle diagram for the two-element induction motor. It is also applicable for the brush-shifting a-c motor for any value of E_{11}. The two points commonly used are the extremities of the no-load and blocked-rotor current vectors. In the two-element induction motor, the diameter through the no-load point is 90 degrees behind the impressed voltage, so the circle can be constructed from these two currents. However, the presence of E_{11} in the brush-shifting a-c motor causes a shift of the diameter of the secondary current locus through the no-load point so that it is no longer perpendicular to E_2 (or the applied voltage). As is seen in Figure 1a, the tangent of the angle of slope of this diameter is

$$\frac{\dfrac{E_{11}}{R}\cos\beta}{\dfrac{{}_{ss}E_2}{X}+\dfrac{E_{11}}{R}\sin\beta} = \frac{\dfrac{E_{11}}{{}_{ss}E_2}\cos\beta}{\dfrac{R}{X}+\dfrac{E_{11}}{{}_{ss}E_2}\sin\beta}$$

where R and X are the total resistance and reactance at standstill referred to the secondary. The voltages and β in this equation may be determined by opening the brush leads and measuring E_{11}, ${}_{ss}E_2$, and $E_{11}+{}_{ss}E_2$. β is determined by forming a triangle of these three voltages. The quantities can also be determined directly if the brush positions and turns ratio are known. The remaining term, R/X, can be evaluated from the blocked rotor current. At standstill, the resultant secondary current, $I_{11}+I_2$, lags the resultant secondary voltage, $E_{11}+E_2$, by an angle the tangent of which is X/R. This is the same angle that I_{1L} lags V_1 at standstill. Hence, X/R is the tangent of the angle between I_{1L} and V_1 at blocked rotor. The corresponding diameter of the primary current locus lags this diameter of the secondary current locus by the angle γ, which was defined earlier.

Erecting a perpendicular bisector to the chord joining the no-load and the blocked rotor current extremities gives another diameter, and the center of the

Figure 1



(a). Secondary currents

(b). Components of primary current which cancel the magnetomotive force of the secondary currents in the stator

(c). Components of primary current which cancel the magnetomotive force of the secondary currents in the adjusting winding

(d). The magnetizing current

(e). The sum of the component currents in the primary

primary-current circle locus lies at the intersection of these two diameters.

This method for determining the locus of the primary current, from the two points and the direction of the diameter through the no-load current extremity, involves the same approximation that is made in ordinary induction-motor theory—that this diameter of the circle passes through the no-load point. Actually, when the machine is running with no output torque, it is loaded with rotational losses. The circle should be constructed with the diameter drawn through the true no-load point, which can be found by supplying these losses mechanically. This refinement produces almost no change in the circle diagram.

The circle can also be located by obtaining three points, rather than by two points and a diameter. This method, which was discussed for specific cases in the earlier papers,[1,2] is valid for all brush positions, since the current locus is always a circle, and three points determine a circle. The necessary data are:

(*a*). The no-load input current and watts at normal voltage.

(*b*). The standstill input current and watts at reduced voltage.

(*c*). The no-load input current and watts at reduced voltage, with the machine running at some speed between the no-load speed and standstill.

This method eliminates the inaccuracies introduced by primary leakage reactance and rotational losses.

Characteristics From the Circle Diagram

When the locus of the extremity of the primary-current vector has been established by the methods discussed above, the characteristics of the motor can be predicted, using methods similar to those used in parts I, II, and III of this series.

In the ordinary induction-motor theory, it is assumed that the total copper loss of the motor is the loss produced by the no-load current plus the loss produced by the load component of current that is reflected from the secondary. The portion of the primary copper loss produced by the no-load current is grouped with the other no-load losses, and the sum of these is assumed to be constant. These assumptions involve two approximations, neither of which seriously affects the accuracy of the method for the two-element induction motor. First, it assumes that the current flowing in the primary is directly proportional to the current in the secondary, so that the division of copper loss between the primary and secondary is the same for all loads. Second, it assumes that the loss for two component currents flowing in a conductor simultaneously is equal to the sum of the losses produced when each component flows separately.

$$(I_{mag}^2+I_{1L}^2)R_1=(I_{mag}+I_{1L})^2R_1$$

This is true only when the component current vectors are in quadrature. Over the operating range of the ordinary induction motor, the magnetizing current is nearly at right angles to the reflected component, I_{1L}, so that the error due to this approximation is small. However, in the brush-shifting motor, the voltage E_{11} may be introduced into the secondary in such a phase position that the phase angle of the reflected current may vary widely with respect to the magnetizing current, introducing quite appreciable errors. The loss curves for the machine can be located without these approximations. Referring to Figure 3, for some general running condition when the input current is pf, the current flowing in the secondary is proportional to of, where po is the magnetizing current. po can be determined by running the machine at no load with the brushes set to make E_{11} zero. The primary copper loss is proportional to $(pf)^2$, and the secondary copper loss is proportional to $(of)^2$. When the rotor is blocked, the total input to the motor is used in supplying losses. Assuming that the sum of the iron, friction, and windage losses remains constant from no load to blocked rotor, the total copper loss can be determined for blocked rotor. From the resistances of the two windings, the division of this loss between the primary and the secondary can be determined. Thus, if the point m is located so that $rm\times V_1$ is the primary copper loss per phase for a primary current pb, and $mb\times V_1$ is the secondary copper loss per phase for a reflected current ob, then for a primary current pf, the primary copper loss per phase is $hk\times V_1$ where

$$hk=rm\frac{pf^2}{pb^2}$$

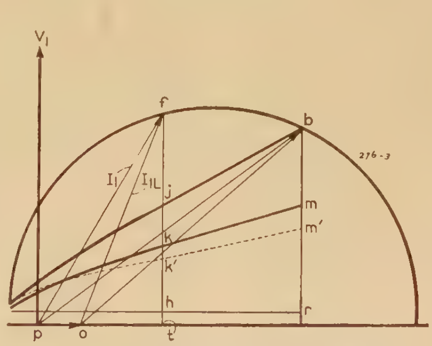


Figure 3. Locus of the primary current, and curves dividing the power component into the parts allocated to the output and the various losses

and the corresponding secondary loss is $kj\times V_1$ where

$$kj=mb\frac{of^2}{ob^2}$$

This method can be used to determine the copper losses at no load. By subtracting these copper losses from the no-load input, the friction, windage, and iron loss is determined.

Using these relations, curves can be constructed that divide the power component of the input current into the portions that are allocated to the output and the various losses. Thus, in Figure 3, for an input current pf:

Input $=tf\times V_1$ watts per phase
Output $=jf\times V_1$ watts per phase
Friction, windage, and iron loss $=th\times V_1$ watts per phase
Primary copper loss $=hk\times V_1$ watts per phase
Secondary copper loss $=kj\times V_1$ watts per phase

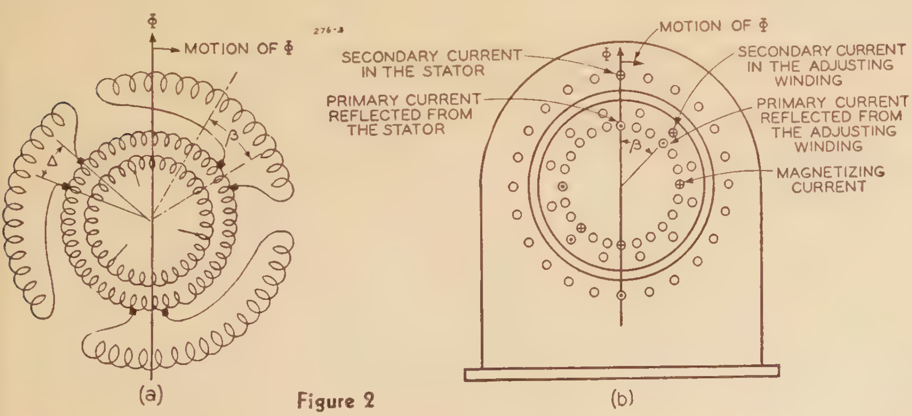


Figure 2

(a). Schematic diagram of the motor, showing the brushes set to retard E_{11} by β degrees. The magnetomotive force of the adjusting winding is β degrees behind that of the stator

(b). Effective currents, representing the magnetomotive forces of the stator, adjusting winding, and primary windings

$$\text{Efficiency} = \frac{if}{tf}$$

$$\text{Power-factor} = \frac{tf}{pf}$$

When E_{11} has no quadrature component with respect to ${}_{ss}E_2$, ($E_{11} \sin \beta = 0$), the slip is equal to the secondary loss divided by the total power across the gap, or

$$S = \frac{{}_{\text{sec}}(I^2R)}{\text{developed power} + {}_{\text{sec}}(I^2R)} \times 100 \text{ per cent of no-load speed}$$

where I is the total secondary current ($I_2 + I_{11}$). Under this condition, all of the I^2R loss in the secondary is supplied by the *speed* voltage, IZ, which varies directly with slip. Appendix B demonstrates that when E_{11} has any phase position β, the voltage supplying the secondary I^2R loss is a combination of a *speed* voltage $(IZ)_s$, which varies directly with slip, and a *transformer* voltage $(IZ)_t = E_{11} \sin \beta$, which is independent of slip. Under this condition, the slip can be evaluated (see appendix B) in terms of the output and the component of the secondary loss, ${}_{\text{sec}}(I^2R)_s$, which is associated with the speed voltage. The expression for the slip is

$$S = \frac{{}_{\text{sec}}(I^2R)_s}{\text{developed power} + {}_{\text{sec}}(I^2R)_s} \times 100 \text{ per cent of } N_o$$

This slip is expressed in per cent of N_o, the no-load speed at which the motor would operate if the brushes were shifted to eliminate the quadrature component ($E_{11} \sin \beta$) without altering the inphase component ($E_{11} \cos \beta$). Similarly, the torque can be shown to be proportional to the component of the total secondary power which is independent of the transformer voltage.

$$\text{Developed power} + {}_{\text{sec}}(I^2R)_s = \frac{2\pi N_o T_{\text{dev}}}{33{,}000} \times 746$$

or

$$T_{\text{dev}} = \frac{33{,}000}{2\pi N_o 746} (\text{developed power} + {}_{\text{sec}}(I^2R)_s)$$

The quantities ${}_{\text{sec}}(I^2R)_s$ and (developed power$+{}_{\text{sec}}(I^2R)_s$) may be evaluated by projecting the components of I_{1L} on V_1 as outlined in appendix B. Also, it can be shown that these quantities may be evaluated from the product of the projections of I_{1L} and V_1 on an axis lagging V_1 by γ degrees, and that ${}_{\text{sec}}(I^2R)_t$, the component of the loss associated with the transformer voltage, can be evaluated from the product of the projections of I_{1L} and V_1 on an axis perpendicular to this axis. Regardless of the method used to evaluate these quantities, it is convenient to draw the dotted line $m'k'$, and so forth, on the circle diagram of Figure 3, so that

$${}_{\text{sec}}(I^2R)_t = k'k \times V_1 \text{ watts per phase}$$
$${}_{\text{sec}}(I^2R)_s = k'j \times V_1 \text{ watts per phase}$$
$$\text{Output} + {}_{\text{sec}}(I^2R)_s = k'f \times V_1 \text{ watts per phase}$$

From this, the slip and torque can be evaluated in a manner which is almost identical with that used for the ordinary induction motor circle diagram.

$$\text{Slip} = \frac{k'j}{k'f} \times 100 \text{ per cent of } N_o$$

$$\frac{2\pi N_o T}{33{,}000} \times 746 = k'f \times V_1 \text{ watts per phase}$$

This involves the same approximation that is made with the ordinary induction motor—that the effect of rotational losses on the slip is negligible.

Experimental Check on the Circle-Diagram Theory

To check the theory that has been presented here, tests were made on the General Electric *BTA* 550–1,650-rpm 4.17–12.5 horsepower 60-cycle brush-shifting motor that was used in the earlier tests.

The brushes were set for two tests so that β was approximately 45 degrees and 135 degrees. The power factor was improved with each of these settings, and the machine ran above synchronous speed when $\beta = 45$ degrees, and below synchronous speed when $\beta = 135$ degrees.

The magnitude and phase position of E_{11} were determined accurately by the voltage measurements described earlier, and from this, from the no-load measurements and from the blocked-rotor measurements, the circles shown in Figure 4a were constructed. It is seen that these predicted circles check the observed points quite closely. Curves of speed and current against torque, and power factor and efficiency against output were constructed from the predicted circle. Figures 4b, c, d, and e show these predicted characteristics compared with those actually observed.

Conclusions

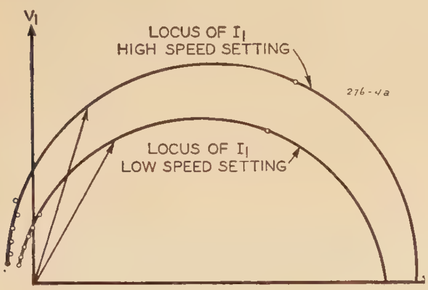
1. Theory and experiments described here have shown that it is possible to adjust the brushes of the brush-shifting a-c motor to correct the power factor of the current supplying it, regardless of the speed to which it is adjusted.

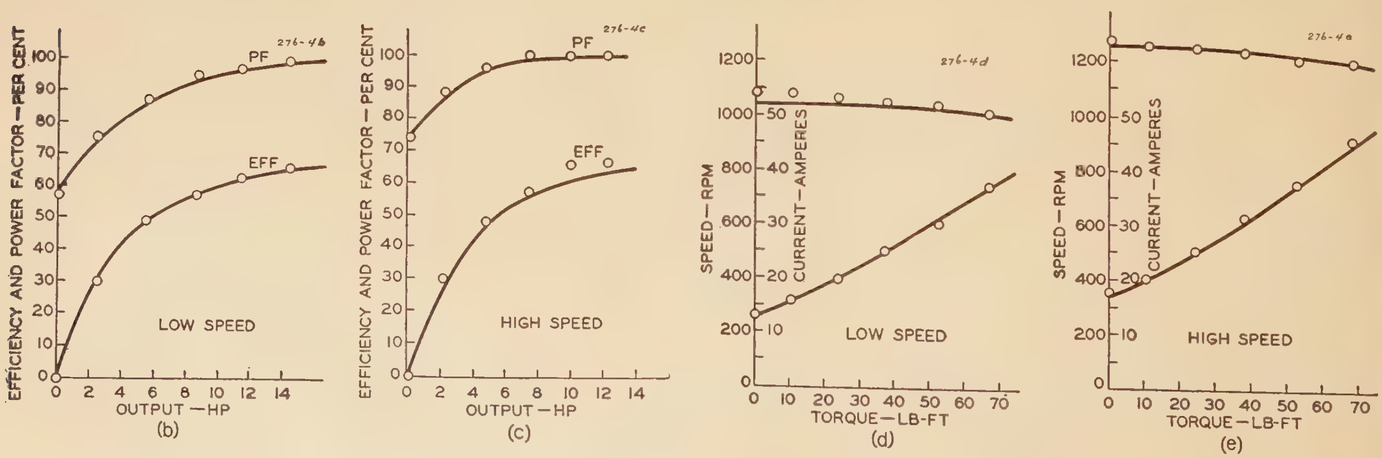
2. The locus of the extremity of the primary current with such adjustments is a circle. The magnitude and location of this circle with respect to the primary-voltage vector can be determined from no-load measurements taken on the motor.

3. Power-factor correction is accomplished

Figure 4. Comparison of observed and predicted characteristics

Points are observed. Lines are predicted





by causing exciting current of the motor to flow in the secondary elements instead of the primary. This causes extra heating in the secondary and reduces the permissible load current that the secondary can carry. In the particular machine used in this investigation values of the quadrature component of E_{11}, (E_{11} sin β) in excess of 10 per cent of the standstill value of the induced secondary voltage E_2 cause excessive secondary currents.

4. While the range of E_{11} sin β (power-factor adjustment) is limited, E_{11} cos β (speed adjustment) is not limited except by the design of the motor. The voltage E_{11} cos β is opposed in normal operation by a speed voltage, E_2, which limits the flow of current produced by it. The quadrature component, E_{11} sin β, causes a secondary current which is opposed only by the motor impedance and not by the speed voltage. Consequently, a small angle β can cause considerable change in power factor in a low impedance motor.

5. A method of determining the characteristics of the motor when used to perform the double function of speed adjustment and

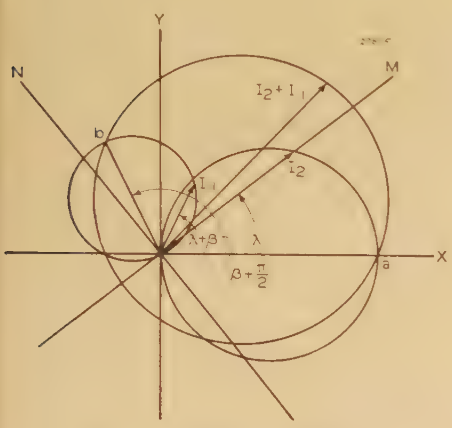


Figure 5. Diagram for appendix A

power-factor correction has been presented. This method employs the theory and use of the circle diagram. The accuracy of the predicted characteristics indicates that the theory and description of the operation of the motor as presented here are essentially correct.

Appendix A. Proof of the Circle Locus of (I_2+I_{11})

The two small circles of Figure 5 with diameters a and b represent the loci of the currents I_2 and I_{11} respectively. Establish the rectangular co-ordinates, x and y, with the x axis along the diameter a. If angles are measured counterclockwise from the x axis, the angle between b and a is $\beta+\pi/2$. A current I_2 at angle λ from the x axis, when added to the corresponding current I_{11} at angle $\lambda+\beta$ from the x axis, results in (I_2+I_{11}). The locus of the extremity of (I_2+I_{11}) is to be expressed in terms of the x, y co-ordinates. To do this it is convenient to establish another set of rectangular co-ordinates m, n rotated counterclockwise by the angle λ from the x, y axis. It is seen that

$$m=I_2+I_{11}\cos\beta=a\cos\lambda+b\sin\lambda\cos\beta$$
$$n=I_{11}\sin\beta=b\sin\lambda\sin\beta$$

By the Pythagorean theorem

$$(I_2+I_{11})^2=(I_2+I_{11}\cos\beta)^2+(I_{11}\sin\beta)^2$$
$$(I_2+I_{11})^2=I_2^2+I_{11}^2+2I_2I_{11}\cos\beta-ab\times\sin\beta\,(\sin^2\lambda+\cos^2\lambda)+ab\sin\beta$$

Since $I_2=a\cos\lambda$ and $I_{11}=b\sin\lambda$

$$\begin{aligned}(I_2+I_{11})^2&=I_2^2+I_{11}^2+2I_2I_{11}\cos\beta-a\sin\beta\times\sin\lambda I_{11}-b\sin\beta\cos\lambda I_2+ab\sin\beta\\&=I_2^2+I_{11}^2+I_2I_{11}\cos\beta-I_2b\cos\lambda\times\sin\beta+I_2I_{11}\cos\beta-a\sin\beta\times\sin\lambda I_{11}+ab\sin\beta\\&=I_2^2+I_{11}^2+I_2b\sin\lambda\cos\beta-I_2b\times\cos\lambda\sin\beta+I_{11}a\cos\lambda\cos\beta-I_{11}a\sin\lambda\sin\beta+ab\sin\beta\\&=I_2^2+I_{11}^2+I_2b\sin(\lambda-\beta)+I_{11}a\times\cos(\lambda+\beta)+ab\sin\beta\\&=I_2[I_2+b\sin(\lambda-\beta)]+I_{11}[I_{11}+a\times\cos(\lambda+\beta)]+ab\sin\beta\\&=I_2[a\cos\lambda+b\sin(\lambda-\beta)]+I_{11}\times\{b\sin[(\lambda-\beta)+\beta]+a\cos(\lambda+\beta)\}+ab\sin\beta\\&=I_2[a\cos\lambda+b\sin(\lambda-\beta)]+aI_{11}\times\cos\lambda\cos\beta-aI_{11}\sin\lambda\sin\beta+I_{11}b\sin(\lambda-\beta)\cos\beta+I_{11}b\times\cos(\lambda-\beta)\sin\beta+ab\sin\beta\end{aligned}$$
$$(I_2+I_{11})^2=[a\cos\lambda+b\sin(\lambda-\beta)](I_2+I_{11}\times\cos\beta)+I_{11}\sin\beta[b\cos(\lambda-\beta)-a\sin\lambda]+ab\sin\beta$$

Since $m=I_2+I_{11}\cos\beta$ and $n=I_{11}\sin\beta$

$$\begin{aligned}(I_2+I_{11})^2&=[a\cos\lambda+b\sin(\lambda-\beta)]m+n\times[b\cos(\lambda-\beta)-a\sin\lambda]+ab\sin\beta\\&=m[\cos\lambda(a-b\sin\beta)+b\sin\lambda\times\cos\beta]+n[-\sin\lambda(a-b\sin\beta)+b\cos\lambda\cos\beta]+ab\sin\beta\end{aligned}$$
$$(I_2+I_{11})^2=(a-b\sin\beta)(m\cos\lambda-n\sin\lambda)+b\cos\beta(m\sin\lambda+n\cos\lambda)+ab\sin\beta$$

Since the co-ordinates x, y are expressed in the m, n co-ordinate system by the equations

$$x=m\cos\lambda-n\sin\lambda$$
$$y=m\sin\lambda+n\cos\lambda$$
$$(I_2+I_{11})^2=(a-b\sin\beta)x+(b\cos\beta)y+ab\times\sin\beta$$

But

$$(I_2+I_{11})^2=x^2+y^2$$

so

$$x^2-x(a-b\sin\beta)+y^2-yb\cos\beta=ab\sin\beta$$

Completing squares

$$\left[x-\left(\frac{a}{2}-\frac{b}{2}\sin\beta\right)\right]^2+\left[y-\frac{b}{2}\cos\beta\right]^2=ab\times\sin\beta+\frac{a^2}{4}-\frac{ab}{2}\sin\beta+\frac{(b\sin\beta)^2}{4}+\frac{(b\cos\beta)^2}{4}$$

$$\left[x-\left(\frac{a}{2}-\frac{b}{2}\sin\beta\right)\right]^2+\left[y-\frac{b}{2}\cos\beta\right]^2=\left[\frac{a}{2}+\frac{b}{2}\sin\beta\right]^2+\left[\frac{b\cos\beta}{2}\right]^2$$

This is the equation of a circle in x, y co-ordinates with center at

$$x=\frac{a}{2}-\frac{b}{2}\sin\beta$$

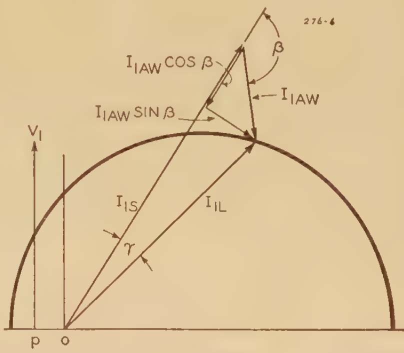
$$y=\frac{b}{2}\cos\beta$$

and radius

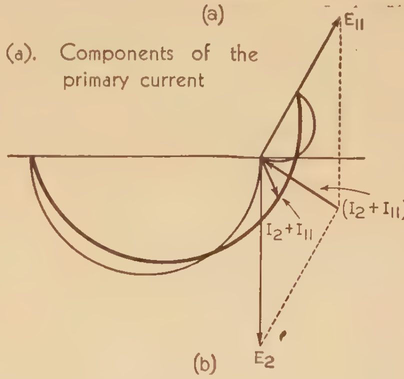
$$r=\left[\left(\frac{a}{2}+\frac{b}{2}\sin\beta\right)^2+\left(\frac{b\cos\beta}{2}\right)^2\right]^{1/2}$$

Appendix B. Determination of Slip and Torque

As is seen from Figure 1, I_{1L} is made up of two components, one of which, I_{1S}, cancels



(a). Components of the primary current



(b). Secondary voltages and currents

Figure 6. Diagrams for appendix B

the magnetomotive force of (I_2+I_{11}) flowing in the stator coils, and the other, I_{1AW}, cancels the magnetomotive force of (I_2+I_{11}) flowing in the adjusting winding. These component currents bear the ratio

$$\frac{I_{1S}}{I_{1AW}}=\frac{N_2}{N_{AW}}=\frac{{}_{ss}E_2}{E_{11}}$$

and are added together at the angle β to form I_{1L} as shown in Figure 6a. Thus, if I_{1L}, β, and ${}_{ss}E_2/E_{11}$ are known, all the component currents are determined.

By projecting the various current vectors in this diagram on the voltage vector, V_1, powers are obtained which can be used in evaluating the speed and torque.

If the vector notation $(I_{1L})\cdot(V_1)$ is used to mean the scalar product of V_1 and the projection of I_{1L} on V_1, and if the primary resistance is assumed to be transferred into the

secondary, the following relations may be written

$$(I_{1L})\cdot(V_1)=(I_2+I_{11})\cdot({}_{ss}E_2+E_{11}) \quad (1)$$
=net power transferred from the primary to the stator and the adjusting windings

$$(I_{1s})\cdot(V_1)=(I_2+I_{11})\cdot({}_{ss}E_2) \quad (2)$$
=power transferred from the primary to the stator

$$(I_{1AW})\cdot(V_1)=(I_2+I_{11})\cdot(E_{11}) \quad (3)$$
=power transferred from the primary to the adjusting winding

$$(I_{1AW}\cos\beta)\cdot(V_1)=(I_2+I_{11})\cdot(E_{11}\cos\beta) \quad (4)$$
=power associated with the component of I_{1AW} which is collinear with I_{1s}

$$(I_{1AW}\sin\beta)\cdot(V_1)=(I_2+I_{11})\cdot(E_{11}\sin\beta) \quad (5)$$
=power associated with the component of I_{1AW} which is in quadrature with I_{1s}

The power described by equation 5 can be identified as a component of the I^2R loss in the machine. Since the resultant secondary voltage is equal to the IZ drop; and the dot product of the resultant current and IZ gives the I^2R loss, we may write

$$E_2+E_{11}=-(I_2+I_{11})Z$$

and

$$(I_2+I_{11})\cdot(E_2+E_{11})=-(I_2+I_{11})^2R$$

(See Figure 6b.) If (E_2+E_{11}) is divided into the quadrature components $(E_2+E_{11}\cos\beta)$ and $(E_{11}\sin\beta)$, the projection of (I_2+I_{11}) on each of these components gives a component of the total copper loss.

$$[(I_2+I_{11})^2R]_s=-(I_2+I_{11})\cdot(E_2+E_{11}\cos\beta) \quad (6)$$

$$[(I_2+I_{11})^2R]_t=-(I_2+I_{11})\cdot(E_{11}\sin\beta) \quad (7)$$

where

$$[(I_2+I_{11})^2R]_s+[(I_2+I_{11})^2R]_t=(I_2+I_{11})^2R$$

But from equation 5, $[(I_2+I_{11})^2R]_t$ is seen to be the product of V_1 and the projection of $(I_{1AW}\sin\beta)$ on V_1, so that $[(I_2+I_{11})^2R]_t$ can be evaluated, and since $[(I_2+I_{11})^2R]$ is known, the remaining component $[(I_2+I_{11})^2R]_s$ can be evaluated.

Obtaining the Torque

The power which is transferred directly from the primary to the stator is, from equation 2

$$(I_{1s})\cdot(V_1)=(I_2+I_{11})\cdot({}_{ss}E_2)$$

Since this power is transferred by a flux which reacts at synchronous speed with the primary

$$(I_{1s})\cdot(V_1)=(I_2+I_{11})\cdot({}_{ss}E_2)=\frac{2\pi N_{\text{sync}}T_{\text{dev}}}{33{,}000}\times746$$

which can be solved for torque. This assumes the more conventional form if it is expressed in terms of N_o rather than N_{sync}, where N_o is the no-load speed when $E_{11}\times\sin\beta$ is made zero, leaving $E_{11}\cos\beta$ unchanged.

$$(I_{1s}+I_{1AW}\cos\beta)\cdot(V_1)=(I_2+I_{11})\cdot({}_{ss}E_2+E_{11}\cos\beta)=\frac{2\pi N_oT_{\text{dev}}}{33{,}000}\times746 \quad (8)$$

This is true because

$$\frac{N_o}{N_{\text{sync}}}=\frac{{}_{ss}E_2+E_{11}\cos\beta}{{}_{ss}E_2}=\frac{I_{1s}+I_{1AW}\cos\beta}{I_{1s}}$$

From equations 1 and 5, it is seen that equation 8 must represent the developed power plus one component of the copper loss

$$\frac{2\pi N_oT_{\text{dev}}}{33{,}000}\times746=P_{\text{dev}}+[(I_2+I_{11})^2R]_s$$

which can be solved for torque.

Obtaining the Speed

Since the slip from synchronous speed, in per cent of synchronous speed is $E_2/{}_{ss}E_2$, the speed can be predicted from E_2. As is seen from Figure 6b

$$E_2=E_{11}\cos\beta+[(I_2+I_{11})Z]_s$$

where $[(I_2+I_{11})Z_s]$ is the component of the impedance drop which is in phase with E_2, so that

$$\frac{E_2}{{}_{ss}E_2}=\frac{E_{11}\cos\beta}{{}_{ss}E_2}+\frac{[(I_2+I_{11})Z]_s}{{}_{ss}E_2}=S_1+S_2$$
=slip in per cent of synchronous speed

where S_1 is the slip caused by $E_{11}\cos\beta$ and S_2 is the slip caused by loading. The first term on the right hand side of this equation is constant for a given brush setting, and the second term varies with torque. This expression can be evaluated more simply if the slip is measured from N_o and expressed in per cent of N_o. This slip is

$$S=\frac{S_2N_{\text{sync}}}{N_o}=\frac{[(I_2+I_{11})Z]_s}{{}_{ss}E_2+E_{11}\cos\beta}\text{ per cent }N_o$$

Here the slip is expressed in terms of the ratio of two voltages. Taking the dot product of these voltages with the secondary currents puts this expression for slip into the conventional form:

$$S=\frac{[(I_2+I_{11})Z]_s\cdot(I_2+I_{11})}{({}_{ss}E_2+E_{11}\cos\beta)\cdot(I_2+I_{11})}=\frac{[(I_2+I_{11})^2R]_s}{P_{\text{dev}}+[(I_2+I_{11})^2R]_s}\text{ per cent }N_o$$

Assumption Regarding Primary Resistance

In deriving the expressions for speed and torque, it was assumed that the primary resistance was replaced by an equivalent resistance in the secondary. Actually, the power consumed in the primary resistance does not cross the air gap. For this reason, the term $[(I_2+I_{11})^2R]_s$ in these expressions must be replaced by the term ${}_{\text{sec}}[(I_2+I_{11})^2R]_s$. This term, which is part of the copper loss in the secondary, was obtained in the manner commonly used for the ordinary induction motor. The division of the loss at blocked rotor was determined by resistance measurements, then it was assumed that the same division of losses occurred at all loads.

List of Symbols

E_{11}—The voltage inserted into the secondary element from the adjusting winding

E_2—The voltage induced in the secondary element by slip

I_{11}—The current flowing in the stator and adjusting winding, resulting from the voltage E_{11}

I_2—The current flowing in the stator and adjusting winding, resulting from the voltage E_2

I—The resultant secondary current I_2+I_{11}

${}_{ss}E_2$—The value of E_2 when the rotor is blocked

I_{mag}—The flux-producing component of the primary current

I_{1L}—The component of the primary current which cancels the magnetomotive forces of the stator and the adjusting windings

I_{1s}—The component of the primary current which cancels the magnetomotive force of the stator winding

I_{1AW}—The component of the primary current which cancels the magnetomotive force of the adjusting winding

I_1—The resultant primary current, or $I_{\text{mag}}+I_{1L}$

V_1—The applied voltage

N_1—The effective primary turns

N_2—The effective stator turns

N_{AW}—The effective adjusting winding turns

β—The angle in electrical degrees
(a). Between E_{11} and ${}_{ss}E_2$
(b). Between the magnetomotive forces of the stator and the adjusting winding
(c). That the brushes must be rotated about the commutator to retard E_{11} by β degrees

γ—The angle in electrical degrees
(a). Between I_{1s} and I_{1L}
(b). Between the magnetomotive force of the stator and the resultant magnetomotive force of the stator and the adjusting winding (c). Between ${}_{ss}E_2$ and $({}_{ss}E_2+E_{11})$

R—The resistance of the primary, secondary, and adjusting winding, reflected to the secondary

X—The standstill reactance of the primary, secondary, and adjusting winding, reflected to the secondary

Z—The secondary and adjusting winding impedance

T_{dev}—The developed torque

P_{dev}—The developed power

$(IZ)_s$—The component of the total secondary and adjusting winding IZ voltage which is in phase with E_2

$(IZ)_t$—The component of the total secondary and adjusting winding IZ voltage which is in quadrature with E_2: $(IZ)_t=-E_{11}\sin\beta$

${}_{\text{sec}}(I^2R)_s$—The component of the secondary I^2R loss which is given by $I\cdot(IZ)_s$

${}_{\text{sec}}(I^2R)_t$—The component of the secondary I^2R loss which is given by $I\cdot(IZ)_t$

N_o—The no-load speed at which the motor

Reactance and Skin Effect of Concentric Tubular Conductors

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THE concentric arrangement of tubular conductors to carry heavy alternating currents gives compactness, low reactance drop, and reduced loss from skin effect or crowding of the current to the surface of the conductors. This arrangement is being used to a considerable extent.[12] In this paper formulas and curves are given to enable the reactance and the skin-effect resistance ratio of such conductors to be determined for three-phase and single-phase circuits.

Reactive Drop

In practical cases the reactance of concentric tubular conductors is very little affected by the variable current density which causes skin effect. When uniform current density is assumed, the "geometric mean distance" method of Clerk Maxwell may be used. Using values given in paragraph 692 of his "Electricity and Magnetism," for geometric mean distances of a tubular section from itself and from other sections, (references 1, 3, and equations 28 and 30 of reference 11) formulas for the reactance drop in each tube of a concentric, three-phase circuit are obtained.

The general formula for reactive drop in conductor 1 of a group of long, parallel conductors is (see reference 11, equation 55, page 39)

$$-j\omega 2\times 10^{-9}[I_1 \text{ logn } D_{s1}+I_2 \text{ logn } D_{12}+ I_3 \text{ logn } D_{13}+\ldots] \text{ volts per centimeter} \quad (1)$$

where

$\omega =$ $2\pi\times$frequency,
logn denotes natural logarithm,
$D_{s1} =$ self geometric mean distance of conductor 1,
$D_{12} =$ geometric mean distance of the cross section of conductor 1 to that of conductor 2, and so forth.

The currents are in amperes. A conductor may have any shape of cross section.

The logarithm of the geometric mean distance between two cross sections is the average of the logarithms of all possible distances between points on one section and points on the other. The logarithm of the self geometric mean distance of the section of a conductor is the average of the logarithms of all possible distances between any two points of the section.

Formula 1 is subject to the condition that

$$I_1+I_2+I_3+I_4+\ldots=0$$

which is a relation usually obtained in any steady-state problem where a system consists of long, parallel conductors, and all the conductors are taken into account. In other words there is as much return current as there is going current in such a system. A system of this kind may be made up of any number of phases and any number of conductors in parallel.

Let the inside and outside diameters of the tubes, from the smallest to the largest, be d_1, d_2, d_3, d_4, d_5, and d_6. Let there be no neutral current.

The reactive drop in the inner tube, which carries I_a amperes, is, in volts per centimeter,

$$\frac{j\omega 2I_a}{10^9}\left[\frac{d_1^4}{(d_2^2-d_1^2)^2}\text{ logn }\frac{d_2}{d_1}-\frac{3d_1^2-d_2^2}{4(d_2^2-d_1^2)}\right]+ \frac{j\omega 2I_b}{10^9}\left[\frac{1}{2}+\frac{d_3^2}{d_4^2-d_3^2}\text{ logn }\frac{d_3}{d_2}-\frac{d_4^2}{d_4^2-d_3^2}\text{ logn }\frac{d_4}{d_2}\right]+ \frac{j\omega 2I_c}{10^9}\left[\frac{1}{2}+\frac{d_5^2}{d_6^2-d_5^2}\text{ logn }\frac{d_5}{d_2}- \frac{d_6^2}{d_6^2-d_5^2}\text{ logn }\frac{d_6}{d_2}\right] \quad (2)$$

The reactive drop in the intermediate tube, which carries I_b amperes, is

$$\frac{j\omega 2I_a}{10^9}\left[\frac{1}{2}-\frac{d_3^2}{d_4^2-d_3^2}\text{ logn }\frac{d_4}{d_3}\right]+ \frac{j\omega 2I_b}{10^9}\left[\frac{d_3^4}{(d_4^2-d_3^2)^2}\text{ logn }\frac{d_4}{d_3}-\frac{3d_3^2-d_4^2}{4(d_4^2-d_3^2)}\right]+ \frac{j\omega 2I_c}{10^9}\left[\frac{1}{2}+\frac{d_5^2}{d_6^2-d_5^2}\text{ logn }\frac{d_5}{d_4}-\frac{d_6^2}{d_6^2-d_5^2}\times \text{ logn }\frac{d_6}{d_4}\right] \text{ volts per centimeter} \quad (3)$$

Reactive drop in the outer tube, which carries I_c amperes, is

$$\frac{j\omega 2(I_a+I_b)}{10^9}\left[\frac{1}{2}-\frac{d_5^2}{d_6^2-d_5^2}\text{ logn }\frac{d_6}{d_5}\right]+ \frac{j\omega 2I_c}{10^9}\left[\frac{d_5^4}{(d_6^2-d_5^2)^2}\text{ logn }\frac{d_6}{d_5}-\frac{3d_5^2-d_6^2}{4(d_6^2-d_5^2)}\right] \text{ volts per centimeter} \quad (4)$$

The phase currents I_a, I_b, and I_c may be unbalanced. Therefore, by putting one of the currents equal to zero, the single-phase case is included.

For deriving the first expression, note that

$$I_a \text{ logn } d_2 = -I_b \text{ logn } d_2 - I_c \text{ logn } d_2$$

since there is no neutral current, and similarly for d_4 in the second expression and for d_6 in the third.

If the thickness of the above tubes is considered negligible, regarding the effect on the reactive drop, and if, as before, there is no neutral current, the reactive drop in the inner tube is

$$-\frac{j\omega 2}{10^9}\left[I_b \text{ logn }\frac{d_4}{d_2}+I_c \text{ logn }\frac{d_6}{d_2}\right] \text{ volts per centimeter} \quad (5)$$

Reactive drop in the intermediate tube is

$$-\frac{j\omega 2}{10^9} I_c \text{ logn }\frac{d_6}{d_4} \text{ volts per centimeter} \quad (6)$$

Reactive drop in the outer tube $=0$. (7)

It does not matter in what units the diameters are given, in equations 2 to 6, so long as they are all in the same units, since only ratios of diameters occur.

For convenience in computation, it may be noted that there are

$$2.540\times 12{,}000 = 3.05\times 10^4 \text{ centimeters in 1,000 feet} \quad (8)$$

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would operate if the brushes were shifted to eliminate E_{11} sin β, leaving E_{11} cos β unchanged

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1. THEORY OF THE BRUSH-SHIFTING A-C MOTOR—I, A. G. Conrad, F. Zweig, J. G. Clarke. AIEE TRANSACTIONS, volume 60, 1941, August section, pages 829–33.

2. THEORY OF THE BRUSH-SHIFTING A-C MOTOR—II, A. G. Conrad, F. Zweig, J. G. Clarke. AIEE TRANSACTIONS, volume 60, 1941, August section, pages 834–6.

3. THEORY OF THE BRUSH-SHIFTING A-C MOTOR—III, A. G. Conrad, F. Zweig, J. G. Clarke. AIEE TRANSACTIONS, volume 61, 1942, July section, pages 502–06.

Also,

$$\text{logn } u = 2.3026 \log_{10} u \tag{9}$$

The resistance drop in each tube is added vectorially to the reactance drop to give the impedance drop in that tube.

Example 1. Find the reactance drop in each tube of the following concentric, three-phase circuit. The inside and outside diameters of the inner tube are 1.2 and 3.0 inches; those of the intermediate tube are 4.0 and 4.5 inches, and those of the outer tube are 5.6 and 6.0 inches. The current of the inner tube is one ampere; that of the intermediate tube is

$$\cos 120° + j \sin 120° = -0.5 + j0.866$$

and that of the outer tube is

$$\cos 240° + j \sin 240° = -0.5 - j0.866$$

The reactive drop in the inner tube is

$$\frac{j\omega 2}{10^9}[0.188 - 0.349(-0.5 + j0.866) - 0.660 \times (-0.5 - j0.866)]$$

$$= \frac{j\omega 2}{10^9}(0.692 + j0.270) \text{ volts per centimeter}$$

to compare with $j\omega 2(0.55 + j\,0.25)/10^9$ by the approximate formula 5.

The reactive drop in the intermediate tube is

$$\frac{j\omega 2}{10^9}(0.164 + j0.253)$$

to compare with $j\omega 2(0.14 + j0.25)/10^9$ by the approximate formula 6.

The reactive drop in the outer tube is

$$\frac{j\omega 2}{10^9}(0.028 + j0.048)$$

to compare with 0 by the approximate formula 7.

For the resistance drop in each tube see example 2.

Skin Effect at Low Frequency

Let there be a tubular conductor of inner radius q and outer radius r, as in Figure 1. Let there be uniform current density a_o abamperes per square centimeter in the tube and let there be a current I_p in a wire at the center of the tube.

Following the procedure of reference 5, beginning at equation 1 of that paper

$$\text{Flux density at } dx = (\text{current inside } dx)\frac{2}{q+x}$$

$$= 2\pi a_o q\left(\frac{2x}{q} - \frac{x^2}{q^2} + \frac{x^3}{q^3} - \frac{x^4}{q^4} + \ldots\right) + \frac{2I_p}{q}\left(1 + \frac{x}{q}\right)^{-1} \tag{10}$$

Integrate from x to t and multiply by $j\omega$ where $\omega = 2\pi \times$ frequency.

Put

$$\frac{4\pi\omega}{\rho} = m^2 \tag{11}$$

where ρ = resistivity of the metal in abohms per centimeter cube. Reactive drop at dx caused by flux inside radius r due to a_o and I_p

$$= \frac{jm^2t^2}{2!}a_o\rho\left[1 - \frac{1}{3}\frac{t}{q} + \frac{1}{4}\frac{t^2}{q^2} - \ldots - \frac{x^2}{t^2} + \frac{1}{3}\frac{x^3}{t^2q} - \frac{1}{4}\frac{x^4}{t^2q^2} + \ldots\right] + \frac{j\omega 2I_p t}{q}\left[1 - \frac{1}{2}\frac{t}{q} + \frac{1}{3}\frac{t^2}{q^2} - \ldots - \frac{x}{t} + \frac{1}{2}\frac{x^2}{tq} - \frac{1}{3}\frac{x^3}{tq^2} + \ldots\right] \tag{12}$$

Let a current density $a_1 + a_1'$ flow, such that its resistance drop will be equal and opposite to the terms in x of equation 12, in order to keep the voltage drop from all causes uniform over the section, as it is in actual fact.

$$a_1 = \frac{jm^2t^2}{2!}a_o\left(\frac{x^2}{t^2} - \frac{1}{3}\frac{x^3}{t^2q} + \frac{1}{4}\frac{x^4}{t^2q^2} - \ldots\right) \tag{13}$$

$$a_1' = \frac{j\omega 2I_p}{q\rho}\left(x - \frac{1}{2}\frac{x^2}{q} + \frac{1}{3}\frac{x^3}{q^2} - \frac{1}{4}\frac{x^4}{q^3} + \ldots\right) \tag{14}$$

These currents will in turn produce flux in the metal and from the terms in x of the resulting voltage drops there are obtained values of current densities a_2 and a_2' and so on. The resulting values are

$$a_2 = \frac{(jm^2t^2)^2}{4!}a_o\left[\frac{x^4}{t_4} - \frac{2}{5}\frac{x^5}{t^4q} + \frac{3}{10}\frac{x^6}{t^4q^2}\ldots\right]$$

$$a_3 = \frac{(jm^2t^2)^3}{6!}a_o\left[\frac{x^6}{t^6} - \frac{3}{7}\frac{x^7}{t^6q} + \frac{9}{28}\frac{x^8}{t^6q^2}\ldots\right]$$

$$a_4 = \frac{(jm^2t^2)^4}{8!}a_o\left[\frac{x^8}{t^8} - \frac{4}{9}\frac{x^9}{t^8q} + \frac{1}{3}\frac{x^{10}}{t^8q^2}\ldots\right]$$

$$a_2' = \frac{jm^2t^2}{3!}\frac{j\omega 2I_p}{\rho}\left[\frac{x^3}{t^2q} - \frac{1}{2}\frac{x^4}{t^2q^2} + \frac{7}{20}\frac{x^5}{t^2q^3} - \frac{11}{40}\frac{x^6}{t^2q^4}\ldots\right]$$

$$a_3' = \frac{(jm^2t^2)}{5!}\frac{j\omega 2I_p}{\rho}\left[\frac{x^5}{t^4q} - \frac{1}{2}\frac{x^6}{t^4q^2} + \frac{5}{14}\frac{x^7}{t^4q^3} - \frac{2}{7}\frac{x^8}{t^4q^4}\ldots\right]$$

$$a_4' = \frac{(jm^2t^2)^3}{7!}\frac{j\omega 2I_p}{\rho}\left[\frac{x^7}{t^6q} - \frac{1}{2}\frac{x^8}{t^6q^2} + \frac{13}{36}\frac{x^9}{t^6q^3} - \frac{7}{24}\frac{x^{10}}{t^6q^4}\ldots\right]$$

Let the total current in the tube be I. By integrating the complete expression for current density over the cross section of the tube

$$I = \pi a_o(2qt + t^2)\left[1 + c_1\frac{jm^2t^2}{3!} + c_2\frac{(jm^2t^2)^2}{5!} + \ldots\right] + I_p\left[d_1\frac{jm^2t^2}{2!} + d_2\frac{(jm^2t^2)^2}{4!} + d_3\frac{(jm^2t^2)^3}{6!} + \ldots\right] \tag{15}$$

where

$$c_1 = 1 - \frac{1}{20}\frac{t^2}{q^2} + \frac{1}{20}\frac{t^3}{q^3} - \frac{11}{280}\frac{t^4}{q^4} + \ldots$$

$$c_2 = 1 - \frac{1}{14}\frac{t^2}{q^2} + \frac{1}{14}\frac{t^3}{q^3} - \ldots$$

$$c_3 = 1 - \frac{1}{12}\frac{t^2}{q^2} + \ldots$$

$$c_4 = 1 - \frac{1}{11}\frac{t^2}{q^2} + \ldots$$

as in reference 5, and where

$$d_1 = 1 + \frac{1}{3}\frac{t}{q} - \frac{1}{12}\frac{t^2}{q^2} + \frac{1}{30}\frac{t^3}{q^3} - \ldots$$

$$d_2 = 1 + \frac{2}{5}\frac{t}{q} - \frac{1}{10}\frac{t^2}{q^2} + \frac{3}{70}\frac{t^3}{q^3} - \ldots$$

$$d_3 = 1 + \frac{3}{7}\frac{t}{q} - \frac{3}{28}\frac{t^2}{q^2} + \frac{1}{21}\frac{t^3}{q^3} - \ldots$$

$$d_4 = 1 + \frac{4}{9}\frac{t}{q} - \frac{1}{9}\frac{t^2}{q^2} + \ldots$$

This gives the value of a_o in terms of I and I_p.

If the tube were not present, the center conductor carrying I_p would supply its resistance loss including its own eddy-current loss. Then, as stated by Waldo V. Lyon[6] at the top of page 1377, AIEE Transactions, 1921, in his paper on eddy-current losses in armature conductors, if the outer tube and its current I be introduced, it does not change the eddy-current loss in the inner conductor, and the total additional power supplied by the circuits to the two conductors is equal to the resistance loss of the tube. There is a transfer of power from the inner conductor to the tube. These amounts of power can be computed.

In the work so far, magnetic flux outside the tube has not been considered as it would not change any of the current densities. It will be included now in computing the transfer of power, and, as would be expected, it will be shown that it has no effect on the expression for eddy-current loss.

The voltage drop in all elements of the tube is the same as that at its outer surface and is

$$j\omega 2(I + I_p)\,\text{logn}\frac{s}{r} + \rho i_{(t)} \tag{16}$$

where $i_{(t)}$ is the current density at the outer surface and where s is a certain large distance to which flux is counted. Since the expression in s will finally cancel out, the result is the same no matter how large a value of s is chosen.

Let conj I be the conjugate of I, that is, the same complex quantity except with j changed to $-j$. Multiply equation 16 by conj I and take the real part, to obtain the power delivered to the tube from its own circuit, per centimeter of the tube. This is

$$\text{Re conj } I\, j\omega 2(I+I_p)\,\text{logn}\,\frac{s}{r}+\text{Re conj } I\, a_o\rho\left[1+b_1\frac{jm^2t^2}{2!}+b_2\frac{(jm^2t^2)^2}{4!}+\ldots\right]+\text{Re conj } I\, I_p\frac{j\omega 4t}{2q+t}\left[e_1+e_2\frac{jm^2t^2}{3!}+e_3\frac{(jm^2t^2)^2}{5!}+\ldots\right] \quad (17)$$

where Re denotes "real part of." Note that in this paper conj applies only to the single letter which follows it, but Re applies to the complete expression which follows.

$$b_1=1-\frac{1}{3}\frac{t}{q}+\frac{1}{4}\frac{t^2}{q^2}-\ldots$$

$$=\frac{1}{2}+\frac{q}{t}-\frac{q^2}{t^2}\,\text{logn}\left(1+\frac{t}{q}\right)$$

$$b_2=1-\frac{2}{5}\frac{t}{q}+\frac{3}{10}\frac{t^2}{q^2}-\frac{17}{70}\frac{t^3}{q^3}\ldots$$

$$b_3=1-\frac{3}{7}\frac{t}{q}+\frac{9}{28}\frac{t^2}{q^2}-\frac{11}{42}\frac{t^3}{q^3}\ldots$$

$$b_4=1-\frac{4}{9}\frac{t}{q}+\frac{1}{3}\frac{t^2}{q^2}\ldots$$

$$b_5=1-\frac{5}{11}\frac{t}{q}+\frac{15}{44}\frac{t^2}{q^2}\ldots$$

as in reference 5.

$$e_1=1+\frac{1}{12}\frac{t^2}{q^2}-\frac{1}{12}\frac{t^3}{q^3}+\frac{3}{40}\frac{t^4}{q^4}-\frac{1}{15}\frac{t^5}{q^5}\ldots$$

$$=\left(\frac{1}{2}+\frac{q}{t}\right)\text{logn}\left(1+\frac{t}{q}\right)$$

$$e_2=1+\frac{1}{10}\frac{t^2}{q^2}-\frac{1}{10}\frac{t^3}{q^3}+\frac{51}{560}\frac{t^4}{q^4}\ldots$$

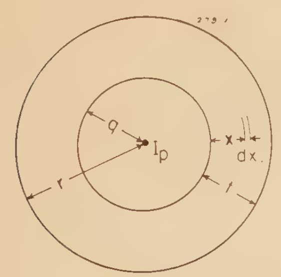

Figure 1. Tubular conductor and central wire

$$e_3=1+\frac{3}{28}\frac{t^2}{q^2}-\frac{3}{28}\frac{t^3}{q^3}\ldots$$

$$e_4=1+\frac{1}{9}\frac{t^2}{q^2}-\frac{1}{9}\frac{t^3}{q^3}\ldots$$

The total flux caused by the current in the tube, as far as radius s, is

$$2I\,\text{logn}\,\frac{s}{r}+4\pi a_o t^2\left[\frac{b_1}{2!}+b_2\frac{jm^2t^2}{4!}+b_3\frac{(jm^2t^2)^2}{6!}+\ldots\right]+\frac{4I_p t}{2q+t}\left[e_2\frac{jm^2t^2}{3!}+e_3\frac{(jm^2t^2)^2}{5!}+\ldots\right] \quad (18)$$

Multiply by $j\omega$ to obtain the voltage induced in the central conductor. Then multiply by conj I_p and take the real part to obtain the additional power supplied by the circuit of the central conductor. This power is transferred to the tube and helps supply the resistance loss in the tube. By adding equation 17, the total resistance loss in the tube is found to be

$$\text{Re conj } I(I)\,j\omega 2\,\text{logn}\,\frac{s}{r}+\text{Re conj } I\,a_o\rho\times\left[1+b_1\frac{jm^2t^2}{2!}+b_2\frac{(jm^2t^2)^2}{4!}+\ldots\right]+\text{Re conj } I\,I_p\frac{j\omega 4t}{2q+t}\times\left[e_1+e_2\frac{jm^2t^2}{3!}+e_3\frac{(jm^2t^2)^2}{5!}+\ldots\right]+\text{Re conj } I_p(I)\,j\omega 2\,\text{logn}\,\frac{s}{r}+\text{Re conj } I_p a_o\rho\left[b_1\frac{jm^2t^2}{2!}+b_2\frac{(jm^2t^2)^2}{4!}+\ldots\right]+\text{Re conj } I_p I_p\frac{j\omega 4t}{2q+t}\left[e_2\frac{jm^2t^2}{3!}+e_3\frac{(jm^2t^2)^2}{5!}+\ldots\right] \quad (19)$$

Now conj II is a real quantity, $|I|^2$, since

$$(a-jb)(a+jb)=a^2+b^2+j0$$

conj $I\,I_p+I$ conj I_p is also a real quantity, since

$$(a-jb)(c+jd)+(a+jb)(c-jd)=2ac+2bd+j0$$

It is the sum of a complex quantity and its conjugate. Therefore, the terms in logn s/r have no real part and disappear.

The resistance loss in the tube, then, is

$$\text{Re conj } Ia_o\rho+\text{Re}(\text{conj } I+\text{conj } I_p)a_o\rho\times\left[b_1\frac{jm^2t^2}{2!}+b_2\frac{(jm^2t^2)^2}{4!}+\ldots\right]+\text{Re}\,\frac{j\omega 4t}{2q+t}\left[\text{conj } I\,I_pe_1+I_p\,(\text{conj } I+\text{conj } I_p)\left\{e_2\frac{jm^2t^2}{3!}+e_3\frac{(jm^2t^2)^2}{5!}+\ldots\right\}\right] \quad (20)$$

Note that the real parts of the entire expressions are taken.

Equations 15 and 20 can be applied to the three tubes in succession of a three-phase concentric circuit, thus obtaining the skin-effect resistance ratio of each tube, that is, the ratio of the resistance loss in the tube compared to the loss with direct current of the same amperage.

The power lost in a tube with direct current of amperage I is

$$\frac{|I|^2\rho}{\pi(2qt+t^2)} \quad (21)$$

The skin-effect resistance ratio of the inner tube is the same as that of an isolated tube, since the surrounding tubes do not affect its current density. In this case, $I_p=0$. The formulas and curves were published in reference 5.

Dividing equation 20 by equation 21 and substituting the value of a_o given by equation 15.

$$\frac{R_{ac}}{R_{dc}}=\text{Re}\,\frac{1+b_1\dfrac{jm^2t^2}{2!}+b_2\dfrac{(jm^2t^2)^2}{4!}+\ldots}{1+c_1\dfrac{jm^2t^2}{3!}+c_2\dfrac{jm^2t^2}{5!}+\ldots} \quad (22)$$

for the inner tube.

For the intermediate tube

$$\frac{R_{ac}}{R_{dc}}=\text{Re}\,\frac{LM}{Q}+\text{Re}S \quad (23)$$

where

$$L=1+\left(1+\frac{\text{conj } I_p}{\text{conj } I}\right)\left\{b_1\frac{jm^2t^2}{2!}+b_2\frac{(jm^2t^2)^2}{4!}+\ldots\right\} \quad (24)$$

$$M=1-\frac{I_p}{I}\left\{d_1\frac{jm^2t^2}{2!}+d_2\frac{(jm^2t^2)^2}{4!}+\ldots\right\} \quad (25)$$

$$Q=1+c_1\frac{jm^2t^2}{3!}+c_2\frac{(jm^2t^2)^2}{5!}+\ldots \quad (26)$$

(Same as equation 16, reference 5, or denominator of 22)

$$S=\frac{I_p}{I}e_1\frac{jm^2t^2}{1!}+\left(\frac{I_p}{I}+\left|\frac{I_p}{I}\right|^2\right)\times\left\{e_2\frac{(jm^2t^2)^2}{3!}+e_3\frac{(jm^2t^2)^3}{5!}+\ldots\right\} \quad (27)$$

In balanced three-phase circuits, for the two opposite phase rotations

$$\frac{I_p}{I}=-\frac{1}{2}+j\frac{\sqrt{3}}{2}\quad\text{or}\quad-\frac{1}{2}-j\frac{\sqrt{3}}{2} \quad (28)$$

$$1+\frac{\text{conj } I_p}{\text{conj } I}=\frac{1}{2}-j\frac{\sqrt{3}}{2}\quad\text{or}\quad\frac{1}{2}+j\frac{\sqrt{3}}{2}$$

$$\frac{I_p}{I}+\left|\frac{I_p}{I}\right|^2=\frac{1}{2}+j\frac{\sqrt{3}}{2}\quad\text{or}\quad\frac{1}{2}-j\frac{\sqrt{3}}{2}$$

The two opposite phase rotations produce slightly different amounts of eddy-cur-

rent loss, that is, different values of R_{ac}, depending on whether the greater current density is produced in the inner or the outer surface of the intermediate tube. See Figure 3.

For the outer tube

$I_p = -I$, and

$$\frac{R_{ac}}{R_{dc}} = \text{Re}\frac{1+d_1\frac{jm^2t^2}{2!}+d_2\frac{(jm^2t^2)^2}{4!}+\ldots}{1+c_1\frac{jm^2t^2}{3!}+c_2\frac{(jm^2t^2)^2}{5!}+\ldots} \qquad (29)$$

Equations 22, 23, and 29 give the skin-effect resistance ratio of the three tubes of a three-phase concentric tubular circuit. They are applicable up to about $mt=4$ and $t/d=0.2(t/q={}^2/_3)$, as in reference 5.

The values of the ratios are plotted in Figure 2 on a base of thickness of copper tube, for standard frequency and temperature. This can save the work of computing by the formulas, in cases where the curves apply. For a frequency f such as 25 or 50 cycles, multiply the tube thickness by $\sqrt{f/60}$ before reading from the curves.

In order to show that there is a larger skin-effect ratio for the intermediate tube than for the others, the current density in the three phases is plotted in Figure 3, as computed by equation 13 and following formulas. The case chosen is that of three flat straps, which is the same as that of three concentric tubes with extremely large radii.

The skin-effect resistance ratio of the outer tube of a single-phase concentric circuit is the same as the ratio for the outer tube of a three-phase concentric circuit as given by Figure 2 or equation 29, since the total current inside the tube in either case is $-I$. The resistance ratio of the inner tube of a single-phase concentric circuit, and also the ratio of an isolated tube, are the same as that of the inner tube of a three-phase circuit, as given by Figure 2 or equation 22. See also references 5 and 7.

Example 2. Find the effective resistance at 60 cycles and 75 degrees centigrade of the three copper tubes described in example 1.

The inner tube, whose inside and outside diameters are 1.2 and 3.0 inches, has a resistance to direct current of 5.47×10^{-8} ohm per centimeter. From Figure 2, taking $t=0.9$ inch and $t/d=0.3$, the skin-effect resistance ratio is 1.92. Then R_{ac} for 60 cycles is

$$5.47\times 10^{-8}\times 1.92 = 1.050\times 10^{-7} \text{ ohm per centimeter}$$

This is to be used for resistance drop and copper loss in the inner tube.

For the intermediate tube, with inside and outside diameters 4.0 and 4.5 inches, and thickness 0.25 inch, the resistance to direct current is 9.74×10^{-8}. From Figure 2, $R_{ac}/R_{dc}=1.059$ and therefore $R_{ac}=1.030\times 10^{-7}$ ohm per centimeter.

For the outer tube, the inside and outside diameters are 5.6 and 6.0 inches. The resistance ratio, from Figure 2, is 1.008 and

$$R_{ac} = 8.92\times 10^{-8}\times 1.008 = 8.99\times 10^{-8} \text{ ohm per centimeter}$$

Bessel-Function Formulas for Single-Phase Concentric Circuits and Isolated Tubes

The well-known solution for skin effect in an isolated tube in terms of Bessel functions is as follows.[2,7]

$$\frac{R_{ac}}{R_{dc}} = \text{Re}\frac{\alpha t(q+r)}{2r}\times \frac{I_o(\alpha r)K_o'(\alpha q)-K_o(\alpha r)I_o'(\alpha q)}{I_o'(\alpha r)K_o'(\alpha q)-K_o'(\alpha r)I_o'(\alpha q)} \qquad (30)$$

where I_o and K_o are modified Bessel functions of the first and second kinds, of order zero, Re denotes real part of

r = outside radius
q = inside radius
t = thickness of tube $= r-q$
$a^2 = \dfrac{j4\pi\omega}{\rho} = jm^2$
$\alpha = me^{\frac{j\pi}{4}}$
$j = \sqrt{-1}$
$\omega = 2\pi\times$frequency
ρ = resistivity of the metal, in abohms per centimeter cube

Note that

$$I_o(\alpha r) = \text{ber } mr + j \text{ bei } mr \qquad (31)$$

$$K_o(\alpha r) = \text{ker } mr + j \text{ kei } mr \qquad (32)$$

$$I_o'(\alpha r) = e^{-j\pi/4}(\text{ber}' \; mr + j \text{ bei}' \; mr) \qquad (33)$$

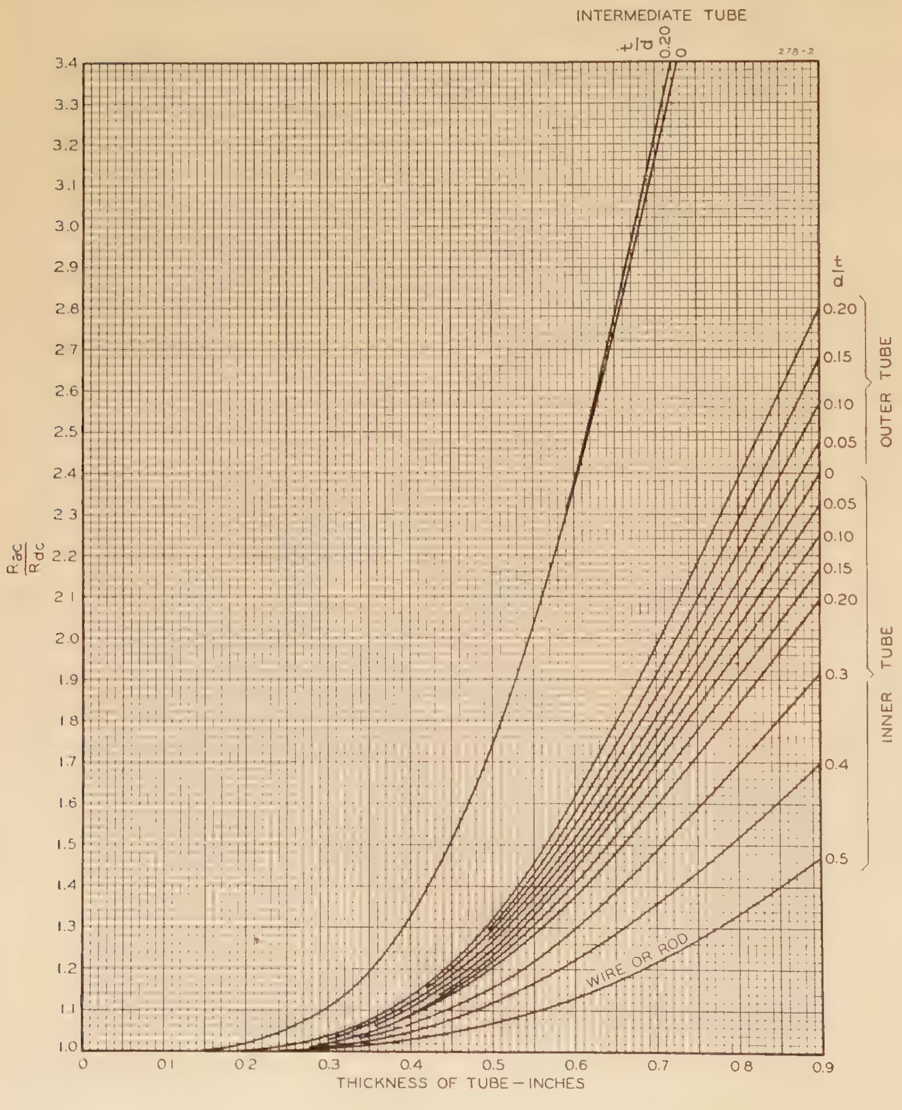

Figure 2. Skin-effect ratios for solid copper tubes, 75 degrees centigrade, 60 cycles, three phase

Thickness $=t$ and outside diameter $=d$, of the tube being considered

$$K_o'(\alpha r) = e^{-j\pi/4}(\text{ker}'\, mr + j\,\text{kei}'\, mr) \qquad (34)$$

Equation 30 is applicable to the inner tube of a concentric circuit, and to a nonconcentric circuit in which the conductors are separated by more than a few diameters and proximity effect is considered negligibly small.

For the outer tube of a concentric circuit, where all the return conductors are within the outer tube[2,4]

$$\frac{R_{ac}}{R_{dc}} = \text{Re}\,\frac{-\alpha t(q+r)}{2q} \times \frac{I_o(\alpha q)K_o'(\alpha r) - K_o(\alpha q)I_o'(\alpha r)}{I_o'(\alpha q)K_o'(\alpha r) - K_o'(\alpha q)I_o'(\alpha r)} \qquad (35)$$

This is the same as formula 30 for an isolated tube, except interchange q and r and multiply by -1. Both formulas 30 and 35 may be derived by following the general method used in references 7 and 4

Formulas 30 and 35 may be used for direct computation of numerical problems, using tabulated values from references 7, 8, 9, or 10.

For very small or very large values of the argument, or for tubes whose thickness is small compared to their radius, series formulas, as given in this paper, may be shorter to use.

High-Frequency Formulas

For frequencies higher than 60 cycles, formulas derived from the asymptotic expansions of Bessel functions are often convenient. Except that they do not give any effect of radiation, and that they apply only to the dominant mode of transmission in a coaxial line, they are applicable to very high frequency since they are series with powers of the frequency in the denominators of the terms. As is usual with power series, their applicability depends on the rapidity with which the terms of the series become smaller and smaller.

Formulas of this type for the inner and

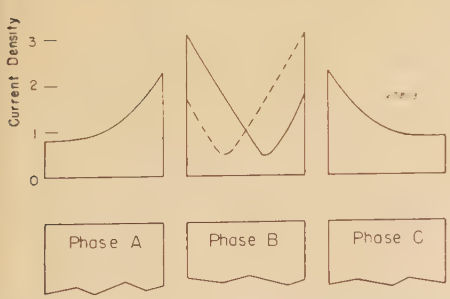

Figure 3. Current density in three-phase strap conductors, closely adjacent

Current density for same amperage of direct current = 1. $mt = \sqrt{6}$. The dotted line is for opposite phase rotation

outer conductors are given here. They are applicable to single-phase, concentric circuits and the formula for the inner conductor is applicable also to all the tubular conductors of circuits which are not concentric and in which the conductors are separated by more than a few diameters. With high frequency, such circuits are more likely to occur than three-phase concentric circuits.

To express equation 30 as a power series using asymptotic expansions (see reference 10, items 810.6, 810.7, 816.1, and 816.2) we have

$$-\frac{I_o'(\alpha q)}{K_o'(\alpha q)} = \frac{I_1(\alpha q)}{K_1(\alpha q)}$$

$$= \frac{e^{2\alpha q}}{\pi}\,\frac{1 - \dfrac{3}{1!8\alpha q} - \dfrac{3\times 5}{2!(8\alpha q)^2} - \dfrac{3^2\times 5\times 7}{3!(8\alpha q)^3}\cdots}{1 + \dfrac{3}{1!8\alpha q} - \dfrac{3\times 5}{2!(8\alpha q)^2} + \dfrac{3^2\times 5\times 7}{3!(8\alpha q)^3}\cdots}$$

$$= \frac{e^{2\alpha q}}{\pi}\left[1 - \frac{6}{8\alpha q} + \frac{18}{(8\alpha q)^2} - \frac{204}{(8\alpha q)^3}\cdots\right]$$

$$\frac{Z_{ac}}{R_{dc}} = \frac{\alpha t(q+r)}{2r}\,\frac{N}{D}$$

where

$$N = e^{\alpha r}\left\{1 + \frac{1}{8\alpha r} + \frac{9}{2(8\alpha r)^2} + \frac{75}{2(8\alpha r)^3}\cdots\right\} + e^{2\alpha q - \alpha r}\left\{1 - \frac{1}{8\alpha r} + \frac{9}{2(8\alpha r)^2} - \frac{75}{2(8\alpha r)^3}\cdots\right\} \times \left\{1 - \frac{6}{8\alpha q} + \frac{18}{(8\alpha q)^2} - \frac{204}{(8\alpha q)^3}\cdots\right\}$$

and

$$D = e^{\alpha r}\left\{1 - \frac{3}{8\alpha r} - \frac{15}{2(8\alpha r)^2} - \frac{105}{2(8\alpha r)^3}\cdots\right\} - e^{2\alpha q - \alpha r}\left\{1 + \frac{3}{8\alpha r} - \frac{15}{2(8\alpha r)^2} + \frac{105}{2(8\alpha r)^3}\cdots\right\} \times \left\{1 - \frac{6}{8\alpha q} + \frac{18}{(8\alpha q)^2} - \frac{204}{(8\alpha q)^3}\cdots\right\}$$

Then

$$\frac{Z_{ac}}{R_{dc}} = \frac{\alpha t(q+r)}{2r}\left[1 + \frac{4}{8\alpha r} + \frac{24}{(8\alpha r)^2} + \frac{192}{(8\alpha r)^3}\cdots + 2e^{-2\alpha t}\left\{1 + \frac{6}{8\alpha r} + \frac{42}{8\alpha r)^2} + \frac{348}{(8\alpha r)^3}\cdots\right\} \times \left\{1 - \frac{6}{8\alpha q} + \frac{18}{(8\alpha q)^2} - \frac{204}{(8\alpha q)^3}\cdots\right\} + \text{terms in } e^{-4\alpha t}\cdots\right] = \frac{t(q+r)}{r^2} \times$$

$$\left[\frac{\alpha r}{2} + \frac{1}{4} + \frac{3}{16\alpha r} + \frac{3}{16\alpha^2 r^2}\cdots + e^{-2\alpha t} \times \left\{\alpha r + \frac{3}{4} + \frac{21}{32\alpha r}\cdots\right\}\left\{1 - \frac{3}{4\alpha q} + \frac{9}{32\alpha^2 q^2}\cdots\right\} + \text{terms in } e^{-4\alpha t}\cdots\right]$$

omitting the final terms of the last two brackets. Now

$$\alpha = me^{j\pi/4} = m\left(\frac{1}{\sqrt{2}} + \frac{j}{\sqrt{2}}\right)$$

$$\frac{1}{\alpha} = \frac{1}{m}e^{-j\pi/4} = \frac{1}{m}\left(\frac{1}{\sqrt{2}} - \frac{j}{\sqrt{2}}\right)$$

$$\frac{1}{\alpha^2} = \frac{1}{m^2}e^{-j\pi/2} = \frac{1}{m^2}(0 - j)$$

$$e^{-2\alpha t} = e^{-mt\sqrt{2}}(\cos mt\sqrt{2} - j\sin mt\sqrt{2})$$

(See reference 10, item 408.05) Taking the real part, the resistance ratio for an isolated tube is

$$\frac{R_{ac}}{R_{dc}} = \frac{t(q+r)}{r^2}\left[\frac{mr}{2\sqrt{2}} + \frac{1}{4} + \frac{3}{16mr\sqrt{2}} + \frac{0}{r^2}\cdots + e^{-mt\sqrt{2}}(\cos mt\sqrt{2})\left\{\frac{mr}{\sqrt{2}} - \frac{3}{4}\,\frac{t}{q} + \frac{3}{32mr\sqrt{2}}\left(7 - 6\frac{r}{q} + 3\frac{r^2}{q^2}\right)\cdots\right\} + e^{-mt\sqrt{2}}(\sin mt\sqrt{2})\left\{\frac{mr}{\sqrt{2}} - \frac{3}{32mr\sqrt{2}} \times \left(7 - 6\frac{r}{q} + 3\frac{r^2}{q^2}\right)\cdots\right\} + \text{terms in } e^{-2mt\sqrt{2}}\cdots\right] \qquad (36)$$

The first line is seen to be the series that is applicable to solid wire. Formula 36 was given a number of years ago in his Master of Science thesis at Massachusetts Institute of Technology by J. M. Roberts, now professor of electrical engineering at the University of Louisville, Louisville, Kentucky.

The expansion of formula 35 for large values of m is not obtained by interchanging q and r in formula 36, for $e^{-mt\sqrt{2}}$ would become $e^{mt\sqrt{2}}$. The expansion is made by taking $K_o(\alpha q)/K_o'(\alpha q)$ as the initial part. The result is:

High-frequency formula for resistance ratio of outer tube:

$$\frac{R_{ac}}{R_{dc}} = \frac{t(q+r)}{q^2}\left[\frac{mq}{2\sqrt{2}} - \frac{1}{4} + \frac{3}{16mq\sqrt{2}} + \frac{0}{q^2}\cdots + e^{-mt\sqrt{2}}(\cos mt\sqrt{2})\left\{\frac{mq}{\sqrt{2}} - \frac{3}{4}\,\frac{t}{r} + \frac{3}{32mq\sqrt{2}}\left(7 - 6\frac{q}{r} + 3\frac{q^2}{r^2}\right)\cdots\right\} + e^{-mt\sqrt{2}}(\sin mt\sqrt{2})\left\{\frac{mq}{\sqrt{2}} - \frac{3}{32mq\sqrt{2}} \times \left(7 - 6\frac{q}{r} + 3\frac{q^2}{r^2}\right)\cdots\right\} + \text{terms in } e^{-2mt\sqrt{2}}\cdots\right] \qquad (37)$$

The Penetration Formula

The "penetration formula" is a well-known and useful method for computing the a-c resistance of conductors at moder-

ately high frequencies. However, special precautions should be taken in using it for tubes, and these will now be described.

A convenient statement of the penetration formula, arranged so as to give a close approximation to the Bessel function solution at high frequencies, is that the alternating current is taken to penetrate, at uniform current density, to a depth δ_1 and the a-c resistance is taken to be equal to the d-c resistance of a tube consisting of the surface layer of metal of thickness δ_1. The formula for the thickness is

$$\delta_1 = \sqrt{2}/m \quad \text{centimeters} \tag{38}$$

where

$$m = \sqrt{(4\pi\omega/\rho)}$$

and where ρ is in abohms per centimeter cube (see equation 11).

For an isolated round wire or tube of outside radius r, the effective resistance, then, is that of a tube of cross section

$$\pi\{r^2-(r-\delta_1)^2\} = 2\pi r\delta_1\left(1-\frac{\delta_1}{2r}\right) = \frac{2\pi r\sqrt{2}}{m}\left(1-\frac{1}{mr\sqrt{2}}\right) \tag{39}$$

from equation 38.

The resistance ratio by the penetration formula is the ratio of the two cross sections, which is

$$\frac{\pi(r^2-q^2)m}{2\pi r\sqrt{2}}\left(1-\frac{1}{mr\sqrt{2}}\right)^{-1} = \frac{t(q+r)}{r^2} \times \left(\frac{mr}{2\sqrt{2}}+\frac{1}{4}+\frac{1}{4mr\sqrt{2}}+\ldots\right) \tag{40}$$

by the binomial expansion of $(1-x)^{-1}$.

The first two terms of the series in equation 40 are the same as in formula 36. These two terms give the equation of the straight line which is the asymptote of the curve of R_{ac}/R_{dc} plotted on mt, as in Figure 3, reference 5.

In order for equation 40 to agree with the corresponding portion of formula 36 within what might be called slide-rule accuracy, or about 0.5 per cent, it is necessary for $1/8m^2r^2$ to be less than 0.005. That is

$$\frac{\delta_1}{r} < \frac{1}{4} \tag{41}$$

This is often expressed by stating that the penetration depth δ_1 should be a small fraction of the radius of curvature, and under such a condition it can apply to conductors that are not round.

In the case of round tubes, another requirement is found from equation 36, namely, that the penetration depth δ_1 should be a small part of the thickness t. Equation 40 plainly does not take care of the terms in $e^{-mt\sqrt{2}}$ of formula 36, and so, if the penetration formula is to be a good approximation, these terms should be proportionately small. This leads to the condition that

$$\frac{\delta_1}{t} < 0.3 \text{ approximately} \tag{42}$$

In most cases, if equation 42 is complied with, then the requirement of equation 41 also is met.

The penetration formula can be applied to the inner surface of the outer tube of a concentric circuit.

Example 3. Find the skin-effect resistance ratio for an outer tube in which $mt=3.2$, $mq=4.8$, and $mr=8$.

$$\frac{q+r}{2q} = \frac{4}{3}, \quad \alpha t = mte^{j\pi/4}$$

$$\begin{aligned} I_o(\alpha q) &= \text{ber } mq + j \text{ bei } mq \\ &= \text{ber } 4.8 + j \text{ bei } 4.8 \\ &= -5.45 + j0.884 \end{aligned}$$

$$\begin{aligned} K_o'(\alpha r) &= e^{-j\pi/4}(\text{ker}'\ 8.0 + j \text{ kei}'\ 8.0) \\ &= e^{-j\pi/4}(-0.000880 - j0.001336) \end{aligned}$$

By equation 35,

$$\frac{R_{ac}}{R_{dc}} = \text{Re}\left\{-3.2(0+j)\times\frac{4}{3}\right\}\frac{-0.447+j0.376}{0.629}$$

$$= 2.55 \text{ by equation 35}$$

From equation 37

$$\frac{t(q+r)}{q^2} = 1.777$$

First line of equation 37

$$= 1.697 - 0.25 + 0.0276$$
$$= 1.475$$

$$mt\sqrt{2} = 4.525 \qquad e^{-4.525} = 0.01083$$

$$\cos 4.525 = -0.1858$$

Second line of equation 37

$$= 0.01083 \times (-0.1858)(3.39 - 0.30 + 0.062\ldots)$$
$$= -0.00634$$

Third line of equation 37

$$= 0.01083 \times (-0.983)(3.39 - 0.062)$$
$$= -0.0354$$

$$\frac{R_{ac}}{R_d} = 1.777(1.475 - 0.0063 - 0.0354)$$
$$= 2.55$$

Thickness of tube in inches, for copper at 75 degrees centigrade and 60 cycles

$$m^2 = \frac{4\pi\omega}{\rho} = \frac{4\pi \times 120\pi}{1{,}724} \times \frac{234+20}{234+75}$$
$$= 2.26$$

$$m = 1.504$$

$$\text{thickness} = \frac{3.2}{1.504} \times \frac{1}{2.54} = 0.838 \text{ inch}$$

$$t/d = \frac{3.2}{2\times 8} = 0.2$$

From Figure 2

$$R_{ac}/R_{dc} = 2.54$$

By the penetration formula, the section of the equivalent tube of thickness δ_1 is

$$\pi\{(q+\delta_1)^2 - q^2\} = 2\pi q\delta_1\left(1+\frac{\delta_1}{2q}\right) = \frac{2\pi q\sqrt{2}}{m}\left(1+\frac{1}{mq\sqrt{2}}\right)$$

by equation 38

$$\frac{R_{ac}}{R_{dc}} = \frac{\pi t(q+r)m}{2\pi q\sqrt{2}\left(1+\frac{1}{mq\sqrt{2}}\right)} = \frac{3.2\times 12.8}{2(6.78+1)} = 2.63$$

which is three per cent too large.

$$\delta_1 = \frac{\sqrt{2}}{1.504} = 0.94 \text{ centimeter or } 0.37 \text{ inch}$$

$$\frac{\delta_1}{t} = \frac{0.37}{0.838} = 0.44$$

which is somewhat too large for the employment of the penetration formula.

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Standardized Load-Center Unit Substations for Low-Voltage A-C Systems

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THE tremendous expansion of production facilities necessitated by American defense and war efforts has given the manufacturer of electrical equipment a serious challenge. Entire large defense projects, such as manufacturing plants, navy yards, drydocks, air depots, and office buildings, have been built in incredibly short times. Any one of these projects will not be complete without an electrical distribution system. One of the many tasks before the electrical manufacturer is to produce the necessary apparatus for the electrical distribution systems, and have it available by the time the project is ready to receive it.

In most of the recent projects power has been distributed to ultimate loads by means of the load-center power distribution systems. This type, although only recently applied to low-voltage systems, has been used successfully for years on high-voltage systems in the central station field. The essentials of load-center distribution for low-voltage a-c systems are

1. A large load area is divided into small load areas, each ranging from 600 to 1,000 kva at 480 volts for example.

2. Power is distributed at medium voltage (2.4 to 13.2 kv) to substations, located near the electrical load center in each of the small areas where the voltage is transformed to utilization voltage to service the area loads.

It has been found that the load-center distribution system when properly applied is superior to other systems from a performance standpoint, and less costly. One of the most important contributions of the electrical manufacturer to the field of low-voltage a-c systems in recent years has been the development and standardization of the load-center unit substation to service the small load areas described above. To the purchaser of electrical apparatus, the development and standardization of the load-center unit substation means that a complete, co-ordinated, pre-engineered step-down substation for a low-voltage a-c system may be ordered in the same manner as a standard motor or standard motor-control equipment.

It is the purpose of this paper to present

1. A definition of a load-center unit substation.

2. The general requirements which have been met by the standardized line with which the writers are most familiar.

3. A description of units with illustrations.

4. Tables for the ready selection of load-center unit substations.

For clarity the term "load-center unit substation" is defined here as follows:

A completely metal-enclosed integrated substation equipment incorporating

1. The required high-voltage switching, control, and incoming circuit termination facilities.

2. Transformer section or sections to transform a-c power from the medium-voltage range (2,400 to 15,000 volts) to the low-voltage range of 600 volts and below.

3. The required low-voltage switching, control, and outgoing feeder circuit termination facilities.

An installation view of a load-center unit substation is shown on Figure 1.

General Considerations

The load-center distribution system is generally thought of in connection with servicing a fairly large area with power. Thus the ultimate areas to be served usually consist of large buildings, mines, yards, docks, piers, camps, and so forth.

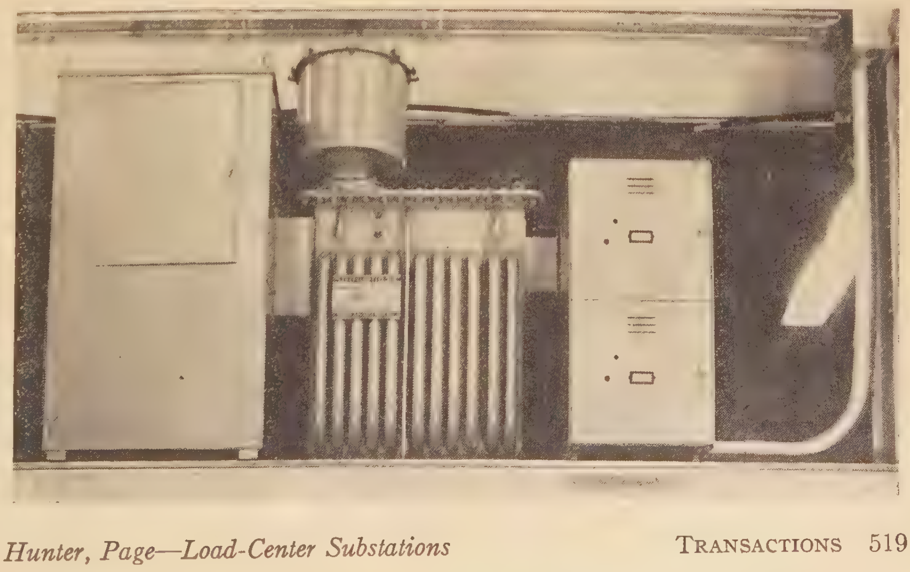
Safety considerations usually limit the highest voltage at which power may be carried within the load area to 13.2 kv. It is usually uneconomical to carry power to load-center locations at less than 2,400 volts because of the large cable required to carry the higher currents. Thus, for a given application any one of the medium voltages may be selected for this service, depending on local conditions and the requirements to be met. Consequently, from the electrical manufacturer's point of view, equipment must be available for the medium voltages, such as 2,400, 4,160, 4,800, 7,200, 12,000, and 13,200 volts.

When the circuit voltage of the available power supply is above 13.2 kv, it is generally advisable to step this down to a voltage in the medium-voltage range for distribution to the load area. This can be conveniently accomplished by means of a multicircuit unit substation, such as is shown on Figure 2. Where many such load areas are to be served, and service reliability is at a premium, two sources of supply may be brought in, or the master substations can be networked to form a primary network. A mobile substation can be used to serve as a spare, similar to the one shown on Figure 3.

From the standpoint of the load-center distribution, there are four types of basic

Figure 1. Indoor installation view of load-center unit substation, Pyranol-filled, rated three-phase, 60 cycles, 100 kva, 2,400 to 208/120 volts

Unit has two sets of primary cutouts for alternate supply and two low-voltage air circuit breakers for feeder protection. Located under crane rails, and supplies power to fluorescent lamps



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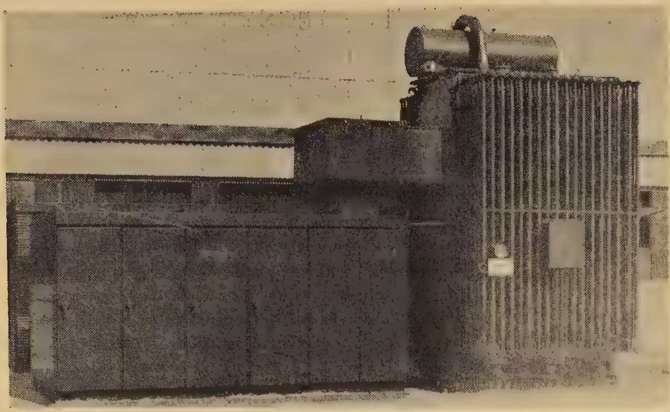

Figure 2. Outdoor unit substation rated three-phase, 60 cycles, 4,500 kva self-cooled, 6,000 kva forced air-cooled, 13,800 to 2,400 volts

Unit has four outgoing feeder circuit breakers and is located next to building it serves

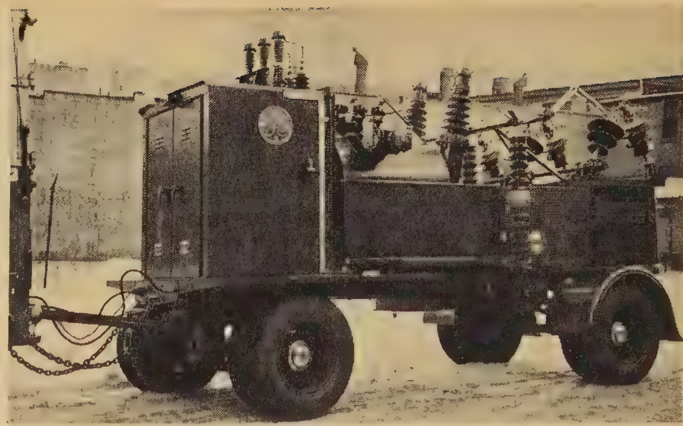

Figure 3. Side view of 1,000-kva mobile substation

circuit arrangements used.[1] These may be described as

1. The straight radial system.
2. The primary-selective system which comprises two primary lines brought into each load-center unit substation, usually with provision for connection to only one line at a time.
3. The radial-primary secondary-selective system, which involves bringing in only one source of primary power, while utilizing normally open secondary tie circuits to other substations to obtain power for emergency operation.
4. The low-voltage network scheme.

Table I. General Requirements of Load-Center Unit Substations

Requirement Item	Rating or Description of Substation
Frequency	Practically all 60 cycles
Phases	Three
Kva ratings	100, 200, 300, 450, 500, 600, 750, 1,000, 1,200, 1,500, 2,000
Primary voltage	2,400, 4,160, 4,800, 7,200, 12,000, 13,200
Secondary voltage	600, 480, 240, 208/120
Transformer coolant	Pyranol, oil, air
Incoming primary feeders	One or two (switches or circuit breakers)
Outgoing secondary feeders	Any number (usually less than ten air circuit breakers)
Enclosures	Indoor: general purpose outdoor: weatherproof
System type	
1. Straight radial	One incoming primary circuit to substation, radial secondary feeders
2. Primary selective	Preferred and emergency incoming primary circuits to substation, radial secondary feeders
3. Secondary selective	One incoming primary circuit to substation, radial secondary feeders with secondary emergency tie circuits to other substations
4. Secondary network	One incoming primary circuit to substation, secondary-network protector, radial secondary feeders, secondary tie circuits

Any one or several of these basic systems may be used in a given application, depending upon the requirements to be met.

The low-voltage ratings of load-center unit substations are fixed by the American Standards Association standard three-phase transformer voltages which are 600, 480, 240, and 208/120 *Y* volts. It has been found that the ASA standard kilovolt-ampere ratings, from 100 through 2,000 kva inclusive, cover nearly 100 per cent of the application requirements for this type of equipment.

The requirements as to location are such that it is essential to have available units for both indoor and outdoor service. These two types allow the distribution engineer to locate the substation in electrical load-center locations, often in space that would otherwise be worthless. An installation view of an outdoor unit is shown on Figure 4.

Thus, the general requirements to be met by such equipment may be tabulated as in Table I.

Description of Equipment

The transformer section of the load-center unit substation consists of a three-phase transformer with leads from the high- and low-voltage windings brought out of the tank through side-wall bushings to metal-enclosed bus connections known as throats. The throats are built integral with the transformer tanks, with the low-voltage throat located on one short-dimension side of the tank and the high-voltage throat located on the opposite side of the tank. The high-voltage throat is arranged so that any one of a number of high-voltage arrangements of switching equipment and cable-termination apparatus may be connected to it, while the low-voltage throat is arranged for connection to metal-enclosed low-voltage switchgear.

The high-voltage equipment may consist of metal-enclosed switchgear. Circuit breakers are available with interrupting ratings from 25,000 to 150,000 kva in 5-kv metal-clad equipments, and 250,000

Figure 4. Outdoor installation view of load-center unit substation, Pyranol-filled, rated three-phase, 60 cycles, 1,500 kva, 13,800 to 575 volts

Unit has primary liquid-filled disconnecting switch and ten low-voltage air circuit breakers for protection of feeders supplying office building. Transformer at right is spare

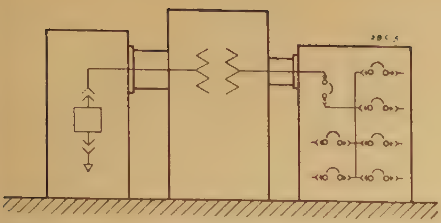

Figure 5. Load-center unit substation with one primary circuit breaker

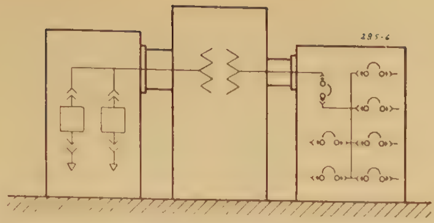

Figure 6. Load-center unit substation with two primary circuit breakers

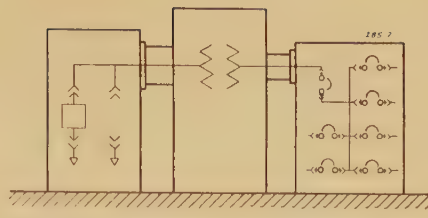

Figure 7. Load-center unit substation with two primary circuit-breaker positions and one circuit breaker

Load-Center Unit-Substation Application Table II. 120/208 Y Volts, Three-Phase

For Use in Connection With Load-Center Unit Substations

Available Primary 3-Phase Short-Circuit Kva	Transformer Kilovolt-Ampere Rating										
	100	150	200	300	450	500	600	750	1,000	1,200	1,500
	Normal Current (Amperes)										
	278	417	556	834	1,250	1,388	1,665	2,080	2,780	3,330	4,164
I. For Transformer Main Low-Voltage Breaker (Selection Based on Adequate Interrupting and Current Rating)											
25,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2	100-AL2
50,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2	100-AL2
75,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2	100-AL2
100,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2	100-AL2
150,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2	100-AL2
250,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2	100-AL2
500,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2	100-AL2
Unlimited	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2	
I. For Transformer Multiple Low-Voltage Breakers (Selection Based on Adequate Interrupting Rating Only. Check Current Ratings From Table V)											
25,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2
50,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2
75,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2
100,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2
150,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	100-AL2	100-AL2
250,000	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	100-AL2	100-AL2
500,000	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2	100-AL2
Unlimited	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2	
III. For Branch Circuit Breakers Co-ordinated With Transformer Main Low-Voltage Breaker (Selection Based on Adequate Interrupting Rating Only. Check Current Ratings From Table V)											
25,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2
50,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
75,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
100,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
150,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
250,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
500,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2
Unlimited	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	
IV. Total Low-Voltage Short-Circuit Currents—Amperes RMS (Impedances as Given in Table V, Total Connected Motor Kilovolt-Amperes Equals 50 Per Cent of Transformer Rating)											
25,000	8,580	12,300	13,300	18,800	26,100	28,300	32,300	35,800	43,500	48,600	55,700
50,000	8,970	13,200	14,200	20,700	29,600	32,400	37,800	42,300	53,300	61,000	71,600
75,000	9,110	13,400	14,600	21,300	31,000	34,100	40,100	45,200	57,800	64,900	79,800
100,000	9,170	13,500	14,800	21,700	31,800	35,000	41,300	46,800	60,400	70,500	84,800
150,000	9,240	13,700	14,900	22,100	32,600	36,000	42,700	48,500	63,300	74,400	90,600
250,000	9,280	13,900	15,000	22,400	33,300	36,800	43,900	50,000	65,900	77,900	95,700
500,000	9,350	14,000	15,100	22,600	33,800	37,500	44,800	51,300	67,900	81,000	100,000
Unlimited	9,380	14,100	15,200	22,900	34,400	38,100	45,900	52,500	70,200	84,100	105,000

1. For different voltage base, multiply values in Table IV by the ratio: Standard voltage/New voltage
2. Transformer contributes approximately 91 per cent of total short-circuit current for 50 per cent connected motors

V. Transformer Impedance Per Cent	100	150	200	300	450	500	600	750	1,000	1,200	1,500
	4.0	4.0	5.0	5.0	5.0	5.0	5.0	5.5	5.5	5.5	5.5

VI. Magnetic Air Circuit Breakers Available

Type	Interrupting Rating (RMS Amperes)	Standard Continuous-Current Trip Ratings (Amperes)
15-AE1A	15,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225
25-AE1B	25,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225, 250, 275, 300, 325, 350, 400, 450, 500, 550, 600
50-AL-2	50,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225, 250, 275, 300, 325, 350, 400, 450, 500, 550, 600, 800, 1,000, 1,200, 1,600
75-AL-2	75,000	2,000, 2,500, 3,000
100-AL-2	100,000	4,000, 5,000, 6,000

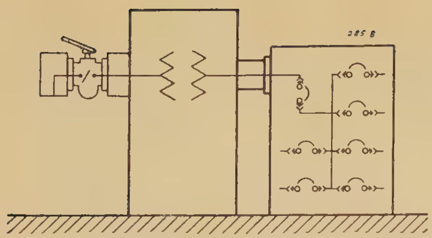

Figure 8. Load-center unit substation with primary flange-mounted liquid-filled cutouts

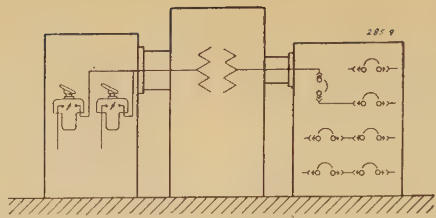

Figure 9. Load-center unit substation with two sets of primary liquid-filled cutouts in free standing enclosure

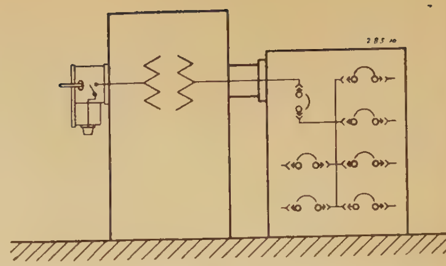

Figure 10. Load-center unit substation with primary liquid-filled flange-mounted switch

Load-Center Unit-Substation Application Table III. 240 Volts, Three-Phase

For Use in Connection With Load-Center Unit Substations Only

Available Primary 3-Phase Short-Circuit Kva	Transformer Kilovolt-Ampere Rating										
	100	150	200	300	450	500	600	750	1,000	1,200	1,500
	Normal Current (Amperes)										
	241	361	481	722	1,083	1,203	1,443	1,804	2,406	2,886	3,609
I.	For Transformer Main Low-Voltage Breakers (Selection Based on Adequate Interrupting and Current Rating)										
25,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2
50,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2
75,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AI 2
100,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2
150,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2
250,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2
500,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2	100-AL2
Unlimited	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2	100-AL2
II.	For Transformer Multiple Low-Voltage Breakers (Selection Based on Adequate Interruping Rating Only. Check Current Ratings From Table V)										
25,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2
50,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2
75,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2
100,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2
150,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2
250,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2
500,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	100-AL2	100-AL2
Unlimited	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2	100-AL2
III.	For Branch Circuit Breakers Co-ordinated With Transformer Main Low-Voltage Breaker (Selection Based on Adequate Interrupting Rating Only. Check Current Ratings From Table V)										
25,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2
50,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
75,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
100,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
150,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
250,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
500,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
Unlimited	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2
IV.	Total Low-Voltage Short-Circuit Current—RMS Amperes (Impedances As Given in Table V, Total Connected Motor Kilovolt-Amperes Equals 100 Per Cent of Transformer Rating)										
25,000	8,040	11,600	12,800	18,200	25,200	27,500	31,600	35,600	43,700	52,500	57,300
50,000	8,370	12,300	13,500	19,720	28,300	31,100	36,300	41,300	52,200	57,000	71,200
75,000	8,470	12,600	13,800	20,300	29,600	32,500	38,300	43,700	56,100	62,700	78,300
100,000	8,550	12,700	14,000	20,600	30,200	33,300	39,400	45,100	58,300	66,200	82,500
150,000	8,610	12,800	14,100	21,000	30,900	34,200	40,600	46,600	60,800	70,000	87,500
250,000	8,650	12,900	14,200	21,200	31,500	34,900	41,700	48,000	63,000	73,700	92,000
500,000	8,700	13,000	14,300	21,500	32,000	35,500	42,400	49,000	64,800	76,800	95,900
Unlimited	8,730	13,100	14,400	21,700	32,500	36,100	43,300	50,100	66,700	80,200	100,000

1. For different voltage base, multiply values in Table IV by the ratio: Standard voltage/New voltage
2. Transformer contributes approximately 84 per cent of total short-circuit current for 100 per cent connected motors

V.	Transformer Impedance Per Cent										
	4.0	4.0	5.0	5.0	5.0	5.0	5.0	5.5	5.5	5.5	5.5

VI. Magnetic Air Circuit Breakers Available

Type	Interrupting Rating (RMS Amperes)	Standard Continuous-Current Trip Ratings (Amperes)
15-AE1A	15,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225
25-AE1B	25,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225, 250, 275, 300, 325, 350, 400, 450, 500, 550, 600
50-AL-2	50,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225, 250, 275, 300, 325, 350, 400, 450, 500, 550, 600, 800, 1,000, 1,200 1,600
75-AL-2	75,000	2,000, 2,500, 3,000
100-AL-2	100,000	4,000, 5,000, 6,000

to 500,000 kva in 15-kv metal-clad equipments. The vertical-lift removable type of breaker is standard in these equipments. Instruments, meters, and relays, mounted on steel panels, may be part of the equipment.

The metal-clad switchgear may have only one breaker as shown in Figure 5. Again there may be two breakers to meet the requirements of the primary-selective scheme as shown in Figure 6. A modification of the primary-selective scheme comprises two breaker positions and only one breaker, as shown in Figure 7. One removable breaker is arranged so that it can be inserted in either of the two breaker positions. This, of course, results in a lower installed cost and has proved attractive for some applications.

In place of the high-voltage metal-enclosed switchgear a set of three flange-mounted gang-operated oil-filled cutout switches may be used, as shown in Figure 8. This type of switch is rated five kilovolts, 250 amperes continuous. Most applications of the switch have utilized the switch as a disconnect only, with copper disconnecting blades in place of fuse links. The cutouts may be fused in the smaller ratings, when co-ordination with the low-voltage circuit breakers can be obtained such that the low-voltage circuit breakers will clear secondary faults before primary fuses are damaged. Cutouts may be fused up to 100 amperes at 5 kv and 200 amperes at 2.5 kv, with inter-

Load-Center Unit-Substation Application Table IV. 480 Volts, Three-Phase

For Use in Connection With Load-Center Unit Substations Only

Available Primary 3-Phase Short-Circuit Kva	Transformer Kilovolt-Ampere Rating											
	100	150	200	300	450	500	600	750	1,000	1,200	1,500	2,000
	Normal Current (Amperes)											
	120	181	241	361	542	601	722	902	1,203	1,443	1,804	2,406
I.	**For Transformer Main Low-Voltage Breakers (Selection Based on Adequate Interrupting and Current Rating)**											
25,000	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2
50,000	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2
75,000	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2
100,000	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2
150,000	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2
250,000	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2
500,000	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2
Unlimited	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2
II.	**For Transformer Multiple Low-Voltage Breakers (Selection Based on Adequate Interrupting Rating Only. Check Current Ratings From Table V)**											
25,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2
50,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2
75,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2
100,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2
150,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2
250,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2
500,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2
Unlimited	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	75-AL2	75-AL2
III.	**For Branch Circuit Breakers Co-ordinated With Transformer Main Low-Voltage Breaker (Selection Based on Adequate Interrupting Rating Only. Check Current Ratings From Table V)**											
25,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B
50,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B
75,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B
100,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2
150,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2
250,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2
500,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2
Unlimited	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2	50-AL2
IV.	**Total Low-Voltage Short-Circuit Current—RMS Amperes (Impedance As Given in Table V, Total Connected Motor Kilovolt-Amperes Equals 100 Per Cent of Transformer Rating)**											
25,000	4,020	5,810	6,390	9,080	12,700	13,800	15,800	17,800	21,800	24,700	28,600	34,400
50,000	4,180	6,150	6,760	9,860	14,200	15,500	18,200	20,600	26,100	30,600	35,600	43,800
75,000	4,250	6,270	6,910	10,200	14,800	16,300	19,200	21,900	28,100	32,600	39,100	49,000
100,000	4,280	6,340	6,980	10,300	15,100	16,700	19,700	22,500	29,200	34,200	41,300	52,200
150,000	4,310	6,400	7,050	10,500	15,500	17,100	20,300	23,300	30,400	35,900	43,800	56,200
250,000	4,330	6,460	7,120	10,600	15,800	17,500	20,800	24,000	31,500	37,400	46,000	59,900
500,000	4,350	6,500	7,160	10,700	16,000	17,800	21,200	24,500	32,400	38,700	47,900	63,000
Unlimited	4,370	6,540	7,200	10,800	16,300	18,100	21,700	25,000	33,400	40,000	50,100	66,800

1. For different voltage base, multiply values in Table IV by the ratio: Standard voltage/New voltage
2. Transformer contributes approximately 84 per cent of total short-circuit current for 100 per cent connected motors

V.	Transformer Impedance Per Cent											
	4.0	4.0	5.0	5.0	5.0	5.0	5.0	5.5	5.5	5.5	5.5	5.5

VI. Magnetic Air Circuit Breakers Available

Type	Interrupting Rating (RMS Amperes)	Standard Continuous-Current Trip Ratings (Amperes)
15-AE1A	15,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225
25-AE1B	25,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225, 250, 275, 300, 325, 350, 400, 450, 500, 550, 600
50-AL-2	50,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225, 250, 275, 300, 325, 350, 400, 450, 500, 550, 600, 800, 1,000, 1,200, 1,600
75-AL-2	75,000	2,000, 2,500, 3,000
100-AL-2	100,000	4,000, 5,000, 6,000

rupting ratings of 5,000 amperes rms at 5 kv, and 10,000 amperes rms at 2.5 kv.

For a five-kilovolt primary-selective system two sets of cutouts are available in a free-standing metal house. All of the cutouts have gang-operating mechanisms, usually interlocked so that only one set can be closed at a time. This arrangement is shown in Figure 9.

A liquid-filled 15-kv disconnecting switch arrangement is shown in Figure 10. This switch is the one developed for network transformers. It is suitable for circuits with normal currents up to 200 amperes. The switch may be filled with either oil or Pyranol, depending upon the application.

In addition to these high-voltage arrangements, the load-center unit substation may be equipped with a cable terminal and junction box without any disconnecting means. These may be oil- or Pyranol-filled for either 5-kv or 15-kv circuits, or air-filled for 5-kv circuits. This arrangement is shown in Figure 11.

The transformer section itself is available in all standard three-phase kilovolt-ampere ratings from 100 to 2,000 kva. Transformers rated above 250 kva are equipped with four $2^1/_2$ per cent taps spaced two above and two below normal high voltage. Automatic voltage-regulating equipment is not included in the transformer section, because there has been no demand for this equipment.

Load-Center Unit-Substation Application Table V. 600 Volts, Three-Phase

For Use in Connection With Load-Center Unit Substations Only

Available Primary 3-Phase Short-Circuit Kva	Transformer Kilovolt-Ampere Rating											
	100	150	200	300	450	500	600	750	1,000	1,200	1,500	2,000
	Normal Current (Amperes)											
	96.2	144	192	289	433	481	577	722	962	1,154	1,444	1,925
I.	**For Transformer Main Low-Voltage Breaker (Selection Based on Adequate Interrupting and Current Rating)**											
25,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2
50,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2
75,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2
100,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2
150,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2
250,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2
500,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2
Unlimited	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2
II.	**For Transformer Multiple Low-Voltage Breakers (Selection Based on Adequate Interrupting Rating Only. Check Current Ratings From Table V)**											
25,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2
50,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2
75,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
100,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
150,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
250,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2
500,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2
Unlimited	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2
III.	**For Branch Circuit Breakers Co-ordinated With Transformer Main Low-Voltage Breaker (Selection Based on Adequate Interrupting Rating Only. Check Current Ratings From Table V)**											
25,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A
50,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B
75,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B
100,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B
150,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B
250,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B
500,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2
Unlimited	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2
IV.	**Total Low-Voltage Short-Circuit Current—RMS Amperes (Impedances As Given in Table V, Total Connected Motor Kilovolt-Amperes Equals 100 Per Cent of Transformer Rating)**											
25,000	3,210	4,640	5,100	7,260	10,100	11,000	12,600	14,200	17,500	19,800	22,900	27,500
50,000	3,350	4,920	5,400	7,870	11,300	12,400	14,500	16,500	20,900	24,100	28,500	35,100
75,000	3,390	5,010	5,520	8,110	11,800	13,000	15,300	17,500	22,500	26,100	31,300	39,200
100,000	3,420	5,060	5,580	8,250	12,100	13,400	15,800	18,100	23,300	27,400	33,100	41,800
150,000	3,450	5,120	5,640	8,380	12,400	13,700	16,300	18,700	24,300	28,700	35,100	45,000
250,000	3,460	5,170	5,680	8,490	12,600	14,000	16,700	19,200	25,300	29,900	36,900	47,900
500,000	3,480	5,200	5,730	8,570	12,800	14,200	17,000	19,600	25,900	31,000	38,400	50,400
Unlimited	3,490	5,220	5,760	8,660	13,000	14,400	17,300	20,000	26,700	32,300	40,100	53,500

1. For different voltage base, multiply values in Table IV by the ratio: Standard voltage/New voltage
2. Transformer contributes approximately 84 per cent of total short-circuit current for 100 per cent connected motors

V.	**Transformer Impedance Per Cent**											
	4.0	4.0	5.0	5.0	5.0	5.0	5.0	5.5	5.5	5.5	5.5	5.5

VI. **Magnetic Air Circuit Breakers Available**

Type	Interrupting Rating (RMS Amperes)	Standard Continuous-Current Trip Ratings (Amperes)
15-AE1A	15,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225
25-AE1B	25,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225, 250, 275, 300, 325, 350, 400, 450, 500, 550, 600
50-AL-2	50,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225, 250, 275, 300, 325, 350, 400, 450, 500, 550, 600, 800, 1,000, 1,200, 1,600
75-AL-2	75,000	2,000, 2,500, 3,000
100-AL-2	100,000	4,000, 5,000, 6,000

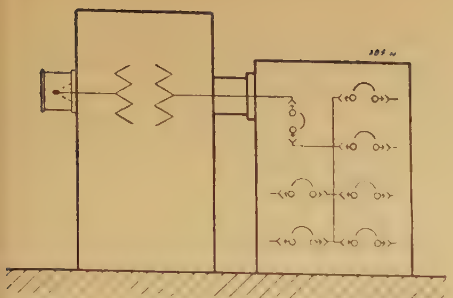

Figure 11. Load-center unit substation with primary junction box only

The transformer may be liquid-filled with the medium of cooling either oil or Pyranol. The Pyranol coolant is generally preferred for both indoor and outdoor load-center applications, but oil-filled transformers are often used outdoors. The Pyranol transformer has been a big factor in the development of the load-center distribution idea. Pyranol will not burn, and thus the Pyranol load-center unit substation can be located indoors near production centers without an expensive fireproof vault.

The dry type of transformer, cooled by the natural circulation of air through the windings, has also been developed for load-center unit substations.

Metal enclosed low-voltage switchgear is used in load-center unit substations. Usually, a number of branch circuits radiate from a single load-center unit substation. These branch circuits are controlled by means of air circuit breakers, mounted in compartments in the low-voltage switchgear. Air circuit breakers, with current interrupting ratings of 15,000, 25,000, 50,000, 75,000, and 100,000 amperes rms, are available in these equipments. The breakers may be either manually or electrically operated. The majority of the applications have required the use of manually operated breakers only. All circuit breakers operate with a time delay on overload currents, but trip instantaneously on short circuit. The breakers may be of either the stationary or the draw-out type. The draw-out type has been used extensively because of the interchangeability of draw-out breakers, which is an important factor in avoiding production delays during routine inspection. The breakers are available in stacks of one, two, three, and four high, depending on the size of the breaker. Two or more stacks can be located side by side to give the required number of breakers. These equipments are used with radial types of systems. For network applications, the standard network protector replaces two of the air circuit breakers in one of the stacks.

Load-Center Unit-Substation Application Tables

The interrupting rating of a circuit breaker is the maximum current which it is able to interrupt adequately and safely. It is important to use circuit breakers of adequate interrupting rating on load-center unit-substation circuits, as well as on any other circuits in which circuit breakers are used, in order to assure reduced arcing time with minimum damage to equipment when faults do occur. Contrary to a former rather widespread belief that low-voltage circuits will not produce short-circuit currents in excess of 20,000 amperes, tests have shown that short-circuit currents of several times this value may occur.[2] These values can be predicted by calculation with reasonable accuracy. Tentative rules for the calculation of these currents have been established by the AIEE.[3]

The load-center unit-substation application tables given here are based on the occurrence of a three-phase secondary fault near the terminals of the unit substation. The only source of power to the secondary, except for secondary motors, is assumed to be through the load-center unit substation. It is assumed that the total connected motor kilovolt-amperes will not exceed 50 per cent of the transformer rating on 208/120-volt systems and 100 per cent of the transformer kilovolt-ampere rating on 240-, 480-, and 600-volt systems. See Tables II, III, IV, and V.

The calculations for the short-circuit currents are based on the following:

1. Systems with voltages of 208/120, 240, 480, and 600 volts, because these are ASA standard voltages given in tentative standards *C*-57.1, dated March 1940.

2. Transformer reactances have been selected to give the most economical balance between transformer reactance and circuit-breaker interrupting rating consistent with reasonable voltage regulation. The actual values of reactance are indicated in the tables.

3. Calculations are based on proposed AIEE standard rules, that is: the symmetrical three-phase rms short-circuit current is increased by a factor of 1.25 to allow for an average d-c component. For the motor contribution, 50 per cent connected motor kilovolt-amperes is assumed to be capable of supplying a short-circuit current of 2.5 times the normal full load current of the transformer, and 100 per cent connected motor kilovolt-amperes is assumed to be capable of supplying a short-circuit current of 5.0 times the normal current of the transformer.

Standard load-center unit-substation breaker arrangements are shown on Figures 12, 13, and 14. The basis of selection of the magnetic air circuit breaker used in the tables for these three arrangements are as follows:

Any air circuit breaker directly connected to the power source (A' or A) can be used up to 100 per cent of its interrupting rating only.

Any magnetic circuit breaker (B) which is backed up by a properly co-ordinated main breaker of adequate interrupting rating (A') can be used up to 200 per cent of its interrupting rating.

Breakers arranged as in Figure 14 are said to be in "cascade."[4] The back-up breaker (A') must be properly co-ordinated with the cascaded breaker (B), so that the breaker (A') will trip instantaneously on short-circuit currents in excess of the interrupting rating of the breaker (B).

The cascade arrangement of air circuit breakers shown in Figure 14 often results in a lower-cost substation than is obtained with the arrangement shown in Figure 13. However, the cascade arrangement has the limitation that a heavy short circuit on any of the branch breakers may open the main air circuit breaker and result in an interruption of service to all of the branch feeders. Thus, when lower cost can be obtained with the cascade arrangement, careful consideration should always be given to the value of continuity of service.

Figure 12. For main breaker A'

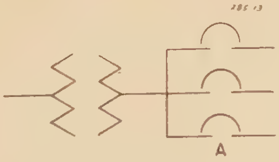

Figure 13. For branch breakers A

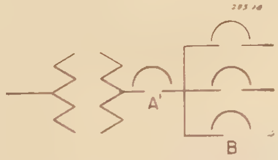

Figure 14. For cascaded branch breakers B

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High-Frequency Coaxial-Line Calculations

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Synopsis: Coaxial cables insulated with solid dielectric are used for transmitting electric energy over wide ranges of frequency. During the development of such cables it was important to be able to predict the characteristics of finished cable by calculations based upon the known dimensions and physical properties of the conductors and the insulation. Many comparatively simple formulas were known to be valid at high frequency, but their applicability at lower frequencies was questioned.

In the first part of this paper we have presented:

1. The general relations which hold under all conditions of operation.

2. The conditions under which "skin-effect" tables offer a convenient method for obtaining values of resistance and inductance, as well as the conditions under which relatively simple equations for these quantities are sufficiently accurate.

3. The conditions under which the complete expression for attenuation may be considerably simplified to the form generally used in high frequency practice.

In the second part of the paper we have presented formulas and graphs for the ratio of terminal voltages as a function of the phase angle of the line for several types of load for both dissipationless and dissipative lines. These relations are helpful in understanding observed voltage variations with frequency caused by reflections, particularly on short lengths of line.

In the third part of the paper a derivation is indicated leading to a general equation for the efficiency of transmission. Simplifications are made which are often applicable in practice, and curves are plotted showing the effect of mismatch in reducing efficiency.

A. Transmission-Line Relations

THE electrical characteristics of a concentric transmission line with distributed constants are given by the relations

Propagation coefficient $=\gamma=$

$$\alpha+j\beta=\sqrt{ZY} \quad (1)$$

Characteristic impedance $=Z_0=\sqrt{Z/Y}$ (2)

where

$\alpha=$attenuation per centimeter length of line
$\beta=$phase shift per centimeter length of line
$Z=R+j\omega L=z\angle\theta_z=$impedance per centimeter length
$Y=G+j\omega C=y\angle\theta_y=$admittance per centimeter length
$R=$series resistance per centimeter length
$L=$series inductance per centimeter length
$G=$parallel conductance per centimeter length
$C=$parallel capacitance per centimeter length

Separating real and imaginary terms in equation 1 we have[1]

$$\alpha=\sqrt{zy}\cdot\cos{}^1/_2(\theta_z+\theta_y)=A\cos(\theta/2) \quad (3)$$

$$\beta=\sqrt{zy}\cdot\sin{}^1/_2(\theta_z+\theta_y)=A\sin(\theta/2) \quad (4)$$

or in terms of the distributed properties of the line

$$\alpha=\sqrt[4]{(RG-\omega^2LC)^2+(G\omega L+R\omega C)^2}\cdot\cos{}^1/_2\left[\tan^{-1}\left(\frac{G\omega L+R\omega C}{RG-\omega^2LC}\right)\right] \quad (5)$$

$$\beta=\sqrt[4]{(RG-\omega^2LC)^2+(G\omega L+R\omega C)^2}\cdot\sin{}^1/_2\left[\tan^{-1}\left(\frac{G\omega L+R\omega C}{RG-\omega^2LC}\right)\right] \quad (6)$$

Also since it is the magnitude of characteristic impedance which is of interest in cable design we have from equation 2

$$Z_0^2=(z/y)=\sqrt{(RG+\omega^2LC)^2+(G\omega L-R\omega C)^2}/(G^2+\omega^2C^2) \quad (7)$$

Equations 1 through 7 are general for any frequency and for any transmission line to which a sinusoidal voltage is applied, so long as the proper coefficients R, L, G, and C are used. We shall therefore consider next the relations by which these four quantities can be calculated.

1. Admittance Per Centimeter Length

Our past experience[2] has shown the convenience of expressing the admittance of a capacitor of any shape as follows:

$$Y=G+j\omega C=\omega C_v(\epsilon''+j\epsilon')$$

from which we have the following definitions of unit quantities characterizing the dielectric

$$\epsilon'=C/C_v=\text{dielectric constant} \quad (8)$$

$$\epsilon''=G/\omega C_v=\text{loss factor} \quad (9)$$

where $C_v=$capacitance of the same size and shape of electrodes in vacuum. For coaxial cylinders of radii r_1 and r_2

$$C_v=0.241\times10^{-12}/\log_{10}(r_2/r_1)\text{ farads/cm} \quad (10)$$

$$\therefore C=0.241\times10^{-12}\epsilon'/\log_{10}(r_2/r_1)\text{ farads/cm} \quad (11)$$

and

$$G=1.515\times10^{-12}f\epsilon''/\log_{10}(r_2/r_1)\text{ mhos/cm} \quad (12)$$

Both coefficients ϵ' and ϵ'' may vary with frequency and must be known or assumed for the insulating material for the frequency range being considered.

2. Impedance Per Centimeter Length

The resistance and the inductance of both the inner and outer conductors will vary with frequency because of "skin effect." Therefore there are no simple general relations which can be used at all frequencies.

If the inner conductor is a round wire, the skin effect at low frequencies can be obtained from Table I, which has been worked out from complicated exact formulas.[3-5] This table gives the ratios R/R_0 and L/L_0 (where R_0 and L_0 represent the resistance and inductance at zero frequency) as functions of the parameter

$$x=2\pi r_1\sqrt{2\mu f/\rho} \quad (13)$$

For copper $\mu=1$, $\rho=1{,}724$ abohmcentimeters and

$$x=0.214r_1\sqrt{f} \quad (14)$$

The depth of penetration y is defined as the thickness of shell to give the same d-c resistance and is given by the relation

$$y=\sqrt{\rho}/2\pi\sqrt{\mu f} \quad (15)$$

or

$$y=6.62/\sqrt{f}\text{ centimeter (for copper)} \quad (16)$$

If the outer conductor is a hollow cylinder of thickness t (where $r_2>5t$), the corresponding values for the ratio R/R_0 were worked out by J. R. Whinnery and are given in Figure 1 as a function of t/y where y is the depth of penetration[6] (equation 16).

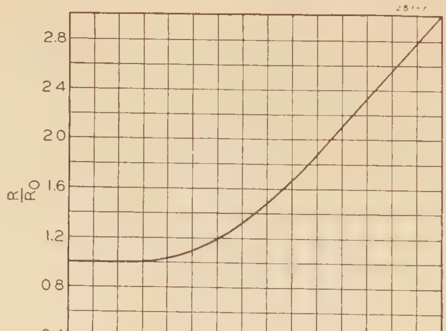

Figure 1. Ratio of a-c to d-c resistance R/R_0 as a function of the ratio of thickness to depth of penetration t/y for a cylindrical outer conductor in a coaxial line

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The resistances of the inner and outer conductors are added together to give the total resistance per unit length

$$R_t=0.548\times10^{-6}\left[\underset{\text{inner}}{\left(\frac{R/R_0}{r_1^2}\right)}+\underset{\text{outer}}{\left(\frac{R/R_0}{2tr_2}\right)}\right]$$
ohm per centimeter length (17)

where (R/R_0) for the inner conductor is taken from Table I and (R/R_0) for the outer conductor is taken from Figure 1.

If the parameter x is greater than 20, a condition which is normally satisfied at high frequency, then the depth of penetration is so small that it is not necessary to use Table I. Likewise if t/y for the outer conductor is greater than 3, then the inner and outer resistances vary inversely as the radii of the conductors as follows:[3]

$$R=\sqrt{\rho\mu f}\left(\frac{1}{r_1}+\frac{1}{r_2}\right)\times10^{-9}\text{ ohm per centimeter length}\quad(18)$$

For copper conductors this reduces to

$$R=4.15\times10^{-8}\sqrt{f}\left(\frac{1}{r_1}+\frac{1}{r_2}\right)$$
ohm per centimeter length (19)

If the inner conductor is stranded and the outer conductor braided, the above calculations will be optimistic, since they do not take into account contact resistance between strands, which may be very important, especially if the copper is dirty or becomes corroded or oxidized. After considerably more empirical evidence is obtained, perhaps multiplying factors can be assigned to indicate a proportionate increase of resistance above the values given by equation 17 caused by stranded inner conductors and braided or wrapped outer conductors. For example, we have found by experience that in coaxial cables built with 0.2-centimeter-diameter inner conductors the attenuation is about one decibel per 100 feet greater at 315 megacycles with a stranded inner conductor than it is with a solid inner conductor.

The expression for inductance[3] at zero frequency is

$$L_0=\left\{4.6\log_{10}(r_2/r_1)+0.5+\left[\frac{4.6r_3^4}{(r_3^2-r_2^2)^2}\log_{10}(r_3/r_2)-\frac{3r_3^2-r_2^2}{2(r_3^2-r_2^2)}\right]\right\}\times$$
10^{-9} henry per centimeter (20)

where r_3 = outer radius of outer conductor in centimeters. The first term gives the inductance between the conductors; the second and third terms give the inductance within the inner and outer conductors respectively.

To show the relative magnitudes of the several terms in equation 20, assume the physical dimensions for cable 1 for which the complete calculations are shown in Table II (next section). Substituting in equation 20 we obtain:

$$L_0=[2.62+0.5+(8.9092-8.8752)]\times$$
10^{-9} henry per centimeter

Both terms of the inductance within the outer conductor were obtained using five-place logarithms, and the numbers have been recorded in order to indicate that the calculation involves a small difference between two nearly equal quantities. However, this also demonstrates that the inductance within the outer conductor may be neglected in comparison with the other terms of equation 20. Since the inductance within the conductors decreases as frequency increases, we have

$$L=[4.6\log_{10}(r_2/r_1)+0.5(L/L_0)]\times$$
10^{-9} henry per centimeter (21)

where L/L_0 for the inner conductor is obtained from Table I. At high frequencies the inductance within the inner conductor also becomes negligible so that we may use the simple relation

$$L=4.6\log_{10}(r_2/r_1)\times10^{-9}\text{ henry per centimeter}\quad(22)$$

3. Approximations Leading to Simplified Formulas

In order to show the conditions under which the general equations 1–7 can be reduced to the commonly used simplified forms, we have shown in Table II the important terms involved for two cables at two frequencies.

Table II shows that at 315 megacycles items 10, 12, and 13 are negligible compared to item 11, that is:

$$\omega^2LC\gg RG\quad(23)$$

and

$$(\omega^2LC)^2\gg(G\omega L+R\omega C)^2\quad(24)$$

If equation 5 is simplified assuming the above inequalities

$$\alpha\cong\sqrt{\omega^2LC}\cdot\cos\frac{\theta}{2}\cong\sqrt{\omega^2LC}\sin\left(\frac{\delta}{2}\right)\cong\sqrt{\omega^2LC}\left(\frac{\delta}{2}\right)\quad(25)$$

where

$$\theta=180^\circ-\delta\quad(26)$$

and

$$\tan\delta\cong\left(\frac{G\omega L+R\omega C}{\omega^2LC}\right)\cong\sin\delta\cong\delta\quad(27)$$

or

$$\alpha\cong(G\omega L+R\omega C)/2\sqrt{\omega^2LC}\quad(28)$$

or

$$\alpha\cong\alpha_d+\alpha_c\cong{}^1/_2G\sqrt{L/C}+{}^1/_2R\sqrt{C/L}\quad(29)$$

where α_d = contribution to attenuation by dielectric loss only

and α_c = contribution to attenuation by copper loss only

α_d may also be obtained directly from equation 5 by assuming $R=0$, and α_c may be obtained by assuming $G=0$ in this same equation. Therefore, we have shown that under the conditions for which equations 23 and 24 are true, the contributions to attenuation of the copper losses and the dielectric losses may be calculated separately and considered to be additive.

Substituting in equation 25 the rela-

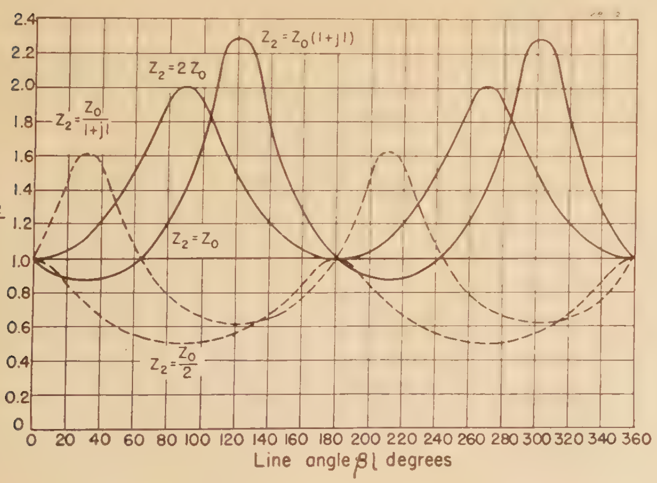
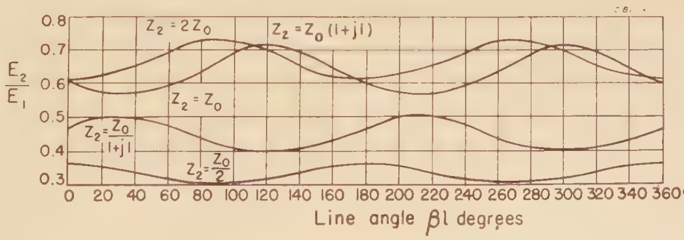


Figure 2. Ratio of receiving to sending voltage E_2/E_1 as a function of line angle βl for several values of terminating impedance Z_2 in a dissipationless line

Figure 3. Ratio of receiving to sending voltage E_2/E_1 as a function of line angle βl for several values of terminating impedance Z_2 for a line having a total attenuation of six decibels



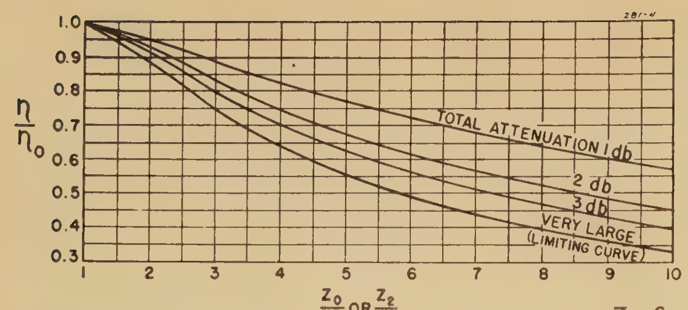


Figure 4. Efficiency of transmission for several values of total attenuation for a range of values of mismatch

$\alpha l = 0.115 \times$ total attenuation of line in decibels

$$\eta = \frac{1}{\cosh 2\alpha l + \frac{1}{2}\left(\frac{Z_0}{Z_2}+\frac{Z_2}{Z_0}\right)\sinh 2\alpha l}$$

Z_0 = Surge impedance (resistive)
Z_2 = Load impedance (resistive)
$\eta_0 = e^{-2\alpha l}$ when $Z_0 = Z_2$

Table I. Skin-Effect Resistance and Inductance Ratios for Solid Round Wires

x	R/R₀	L/L₀	x	R/R₀	L/L₀
0.0	1.00000	1.00000	2.5	1.17538	0.91347
0.1	1.00000	1.00000	2.6	1.20056	0.90126
0.2	1.00001	1.00000	2.7	1.22753	0.88825
0.3	1.00004	0.99998	2.8	1.25820	0.87451
0.4	1.00013	0.99993	2.9	1.28644	0.86012
0.5	1.00032	0.99984	3.0	1.31809	0.84517
0.6	1.00067	0.99966	3.5	1.49202	0.76550
0.7	1.00124	0.99937	4.0	1.67787	0.66632
0.8	1.00212	0.99894	4.5	1.36275	0.61563
0.9	1.00340	0.99830	5.0	2.04372	0.55597
1.0	1.00519	0.99741	6.0	2.39359	0.46521
1.1	1.00758	0.99821	7.0	2.74319	0.40021
1.2	1.01071	0.99465	8.0	3.09445	0.55107
1.3	1.01470	0.99266	9.0	3.44638	0.31257
1.4	1.01988	0.99017	10.0	3.79857	0.28162
1.5	1.02582	0.98711	11.0	4.15100	0.25622
1.6	1.03323	0.98342	12.0	4.50358	0.23501
1.7	1.04203	0.97904	13.0	4.85831	0.21703
1.8	1.05240	0.97390	14.0	5.20915	0.20160
1.9	1.06440	0.96795	15.0	5.56205	0.18822
2.0	1.07818	0.96113	20.0	7.32767	0.14128
2.1	1.09375	0.95343	25.0	9.09412	0.11307
2.2	1.11126	0.94482	30.0	10.88101	0.09424
2.3	1.13069	0.93527	40.0	14.39545	0.07069
2.4	1.15207	0.92482	50.0	17.93032	0.05656
			60.0	21.46541	0.04713
			80.0	28.53593	0.03535
			100.0	35.60666	0.02828
					0.00000

$$\lim_{x\to\infty}\left(\frac{R}{R_0}\right) = 0.354x$$

Table II. Important Items in Equation 5

	Cable 1		Cable 2	
Cable specifications				
r_1	0.1028 cm		0.325 cm	
r_2	0.381 cm		1.27 cm	
t	0.025 cm		0.025 cm	
ϵ'	2.04		2.65	
ϵ''	0.001		0.0089	
Frequency (cycles per second)	2.5×10^4	3.15×10^8	2.5×10^4	3.15×10^8
1. C (eq 11)	1.017×10^{-12}	1.017×10^{-12}	1.08×10^{-12}	1.08×10^{-12}
2. G (eq 12)	6.65×10^{-11}	8.39×10^{-7}	1.85×10^{-10}	2.34×10^{-6}
3. x (eq 14)	3.48	(590)	11.0	(1,230)
4. R/R_0	1.485	(209)	4.15	(435)
5. R (eq 17)	1.06×10^{-4}		0.303×10^{-4}	
6. R (eq 19)	(0.81×10^{-4})	91.0×10^{-4}	(0.254×10^{-4})	28.5×10^{-4}
7. L/L_0	0.769		0.256	
8. L (eq 20)	3.00×10^{-9}		2.85×10^{-9}	
9. L (eq 22)	(2.62×10^{-9})	2.62×10^{-9}	(2.72×10^{-9})	2.72×10^{-9}
10. RG	7.05×10^{-15}	7.63×10^{-9}	5.60×10^{-15}	6.67×10^{-9}
11. ω^2LC	7.52×10^{-11}	1.045×10^{-2}	7.58×10^{-11}	1.15×10^{-2}
12. $G\omega L$	3.13×10^{-14}	4.35×10^{-6}	8.27×10^{-14}	1.26×10^{-5}
13. $R\omega C$	1.69×10^{-11}	1.83×10^{-5}	5.14×10^{-12}	6.09×10^{-6}
14. A (eqs 3 and 5)	8.8×10^{-6}		8.7×10^{-6}	
15. ϵ (eqs 3 and 5)	167.3		176.1	
16. cos $(\theta/2)$	0.11		0.034	
17. α (eq 5)	9.69×10^{-7}		2.96×10^{-7}	
18. α_c (eq 30)	(8.0×10^{-7})	9.0×10^{-5}	(2.52×10^{-7})	2.83×10^{-5}
19. α_d (eq 32)	(1.7×10^{-9})	2.14×10^{-5}	(4.67×10^{-9})	5.9×10^{-6}
20. α (eq 29)	(8.02×10^{-7})	11.1×10^{-5}	(2.57×10^{-7})	8.7×10^{-5}
21. N_{db}/100 ft	2.56×10^{-2}	2.94	7.35×10^{-8}	2.31
Error using approximate equation		−17%		−13%

tions for R, L, C, and G given in equations 19, 22, 11, and 12 respectively, we have

$$\alpha_c \cong 1.50\times10^{-10}\sqrt{f\epsilon'}\left(\frac{1}{r_1}+\frac{1}{r_2}\right)/\log_{10}\times (r_2/r_1) \text{ neper per centimeter} \quad (30)$$

or

$$N_c \cong 3.98\times10^{-6}\sqrt{f\epsilon'}\left(\frac{1}{r_1}+\frac{1}{r_2}\right)/\log_{10}\times (r_2/r_1) \text{ decibel per 100 feet} \quad (31)$$

and

$$\alpha_d \cong 1.05\times10^{-10} f\epsilon''/\sqrt{\epsilon'} \text{ neper per centimeter} \quad (32)$$

or

$$N_d \cong 2.78\times10^{-6} f\epsilon''/\sqrt{\epsilon'} \text{ decibel per 100 feet} \quad (33)$$

where

$$N_{\text{total}} \cong N_c(\text{copper loss only}) + N_d \text{ (dielectric loss only)} \quad (34)$$

If equation 7 is simplified assuming the inequalities, equations 23 and 24, we have

$$Z_0 = \sqrt{L/C} = 138 \log_{10}(r_2/r_1)/\sqrt{\epsilon'} \quad (35)$$

which is the expression usually used for characteristic impedance at high frequency.

Likewise assuming the inequalities, equations 23 and 24, equation 6 reduces to

$$\beta \cong \sqrt{\omega^2 LC} \cong \omega C Z_0 \quad (36)$$

The trigonometric term in equation 5 is important, because it is zero if the losses are zero. Thus an approximate evaluation at high frequencies for this term (although it is small) leads to the useful relation given in equation 25.

On the other hand, the trigonometric term in equation 6 is unimportant, since it is unity for zero losses, and losses at high frequencies simply decrease this term slightly below unity. The recognized effect of high losses in increasing the phase constant must appear, because some of the factors in the first term of equation 6 are not negligible compared to $\omega^2 LC$. However, when the inequalities, equations 23 and 24, are true, the losses do not appreciably increase the phase constant.

Similarly equation 29 can be expressed in terms of Z_0

$$\alpha \cong (GZ_0/2) + (R/2Z_0) \quad (37)$$

The frequency above which the simplified relations, equations 25–37, can be used depends upon the error which can be tolerated. Table II shows that appreciable errors result if the approximate relations are used at 25 kilocycles. However these relations should be sufficiently

accurate for frequencies in the broadcast band or above.

B. Voltage Relations

In a transmission line of length l, having uniformly distributed constants, the complex expression for the ratio of the voltage at the sending end E_1 to the voltage at the receiving end E_2 is given by the relation

$$\frac{E_1}{E_2} = \cosh \gamma l + \frac{Z_0}{Z_2} \sinh \gamma l \quad (38)$$

which upon expansion becomes

$$\frac{E_1}{E_2} = \left(\cosh \alpha l + \frac{Z_0}{Z} \sinh \alpha l\right) \cos \beta l + j\left(\sinh \alpha l + \frac{Z_0}{Z_2} \cosh \alpha l\right) \sin \beta l \quad (39)$$

where Z_0 = surge impedance of line
and Z_2 = impedance across receiving end of line

From this general relation several special cases lead to usable simplifications.

1. Dissipationless Case

$$\begin{pmatrix} G = R = \sinh \alpha l = 0 \\ \cosh \alpha l = 1,\ Z_0 = \text{real} \end{pmatrix}$$

(*a*). Assuming pure resistance termination, (Z_2 = real), the absolute value of the voltage ratio given by equation 39 can be reduced to

$$\left|\frac{E_1}{E_2}\right| = \sqrt{\frac{1}{2}\left[1+\left(\frac{Z_0}{Z_2}\right)^2\right] + \frac{1}{2}\left[1-\left(\frac{Z_0}{Z_2}\right)^2\right] \cos 2\beta l} \quad (40)$$

(*b*). Assuming complex termination ($Z_2 = \sqrt{R_2^2 + x_2^2}$) equation 39 becomes

$$\left|\frac{E_1}{E_2}\right| = \sqrt{\frac{1}{2}\left[1+\left(\frac{Z_0}{Z_2}\right)^2\right] + \frac{1}{2}\left[1-\left(\frac{Z_0}{Z_2}\right)^2\right] \cos 2\beta l + \frac{X_2 Z_0}{Z_2^2} \sin 2\beta l} \quad (41)$$

The last two equations have been used to calculate the data given in Figure 2 which show the ratio of receiving to sending voltage E_2/E_1 for the dissipationless line for several types of load impedance.

2. Dissipative Line

For a line having high losses, particularly at low frequency, the surge impedance (Z_0) may have an appreciable reactive component. But in lines for which equations 23 and 24 apply, the reactive component of Z_0 is negligible as indicated by equation 35.

(*a*). Assuming pure resistance termination (Z_2=real) equation 39 reduces to

$$\left|\frac{E_1}{E_2}\right| = \sqrt{\frac{Z_0}{Z_2} \sinh 2\alpha l + \frac{1}{2}\left[1+\left(\frac{Z_0}{Z_2}\right)^2\right] \cosh 2\alpha l + \frac{1}{2}\left[1-\left(\frac{Z_0}{Z_2}\right)^2\right] \cos 2\beta l} \quad (42)$$

(*b*). Assuming complex termination, ($Z_2 = \sqrt{R_2^2 + x_2^2}$) equation 39 becomes

$$\left|\frac{E_1}{E_2}\right| = \sqrt{\frac{RZ_0}{Z_2^2} \sinh 2\alpha l + \frac{1}{2}\left[1+\left(\frac{Z_0}{Z_2}\right)^2\right] \cosh 2\alpha l + \frac{1}{2}\left[1-\left(\frac{Z_0}{Z_2}\right)^2\right] \cos 2\beta l + \frac{X_2 Z_0}{Z_2^2} \sin 2\beta l} \quad (43)$$

Figure 3 shows plots of the last two relations for a total attenuation of six decibels for the same terminating impedances as were used in Figure 2.

3. High-Loss Lines

As the total attenuation is increased, the variation in the ratio of E_2/E_1 with line angle decreases. For a line having an attenuation of 20 decibels, αl=2.3, and there is only about two per cent error in the relation

$$\cosh \alpha l = \sinh \alpha l = {}^1/_2 \epsilon^{\alpha l} \quad (44)$$

Under these conditions equation 39 can be further simplified.

(*a*). Assuming pure resistance termination (Z_2 = real)

$$\left|\frac{E_1}{E_2}\right| = \frac{\epsilon^{\alpha l}}{2}\left(1+\frac{Z_0}{Z_2}\right) \quad (45)$$

(*b*). Assuming complex termination ($Z_2 = \sqrt{R_2^2 + x_2^2}$)

$$\left|\frac{E_1}{E_2}\right| = \frac{\epsilon^{\alpha l}\sqrt{Z_2^2 + 2R_2 Z_0 + Z_0^2}}{2Z_2} \quad (46)$$

The simplest relation for the ratio E_1/E_2 is obtained when the line is terminated in a load which is exactly equal to its surge impedance ($Z_2 = Z_0$) in which case

$$\left|\frac{E_1}{E_2}\right| = \epsilon^{\alpha l} \quad (47)$$

Equations 38–47 were derived in order to interpret voltage data taken during measurements of attenuation in commercial high-frequency solid dielectric coaxial cables.[7]

C. Efficiency of Transmission

The efficiency of transmission is defined as the ratio of output to input power; however, the mathematical expression is simpler if the reciprocal relations are used. The most general case is that for which both the characteristic and load impedances of the line are complex. This case has been solved by E. W. Hamlin (unpublished report, General Electric Company). The essential steps and final relation are:

$$\text{Characteristic impedance} = Z_0 = R_0 + jX_0 \quad (48)$$

$$\text{Load impedance} = Z_2 = R_2 + jX_2 \quad (49)$$

then the input impedance is given by[8]

$$Z_1 = Z_0\left[\frac{Z_2 \cosh \gamma l + Z_0 \sinh \gamma l}{Z_0 \cosh \gamma l + Z_2 \sinh \gamma l}\right] \quad (50)$$

and the input current as a function of the load current is given by

$$I_1 = \frac{I_2}{Z_0}(Z_0 \cosh \gamma l + Z_2 \sinh \gamma l) \quad (51)$$

$$\text{The input power} = |I_1|^2 \times \text{real component of } Z_1 \quad (52)$$

$$\text{and the output power} = |I_2|^2 R_2 \quad (53)$$

Hamlin takes the ratio of these last two relations and upon expansion and simplification obtains the general equation:

$$\frac{W_1}{W_2} = \frac{R_0}{R_2|Z_0|^2}[(R_2R_0 + X_2X_0) \cosh 2\alpha l + {}^1/_2(|Z_2|^2 + |Z_0|^2) \sinh 2\alpha l] + \frac{X_0}{R_2|Z_0|^2}[(X_0R_2 - X_2R_0) \cos 2\beta l + {}^1/_2(|Z_2|^2 - |Z_0|^2) \sin 2\beta l] \quad (54)$$

As a simplification of equation 54, let X_0=0, that is, assume that the reactive component of the characteristic impedance is negligible, as is usually the case in high-frequency transmission. Then equation 54 reduces to

$$\frac{W_1}{W_2} = \cosh 2\alpha l + {}^1/_2\left(\frac{R_2}{R_0} + \frac{R_0}{R_2} + \frac{X_2^2}{R_2R_0}\right) \sinh 2\alpha l \quad (55)$$

When the reactive component of the load impedance is also negligible, the last term in the parenthesis drops out.

The last equation has been used to calculate the efficiency of transmission for several values of total attenuation for a range of values of mismatch. (Z_0/Z_2 or Z_2/Z_0 from 1 to 10) as shown in Figure 4. An important observation is that for a mismatch as large as 2/1, the efficiency is 90 per cent or more of the efficiency which could be obtained with perfect matching of characteristic and load impedances.

The curves of Figure 4 express only the increase in power losses in the line caused by standing waves on the line resulting from reflections at the load end. Unless the attenuation of the line is high, the

Calorimetric Method for Determining Efficiencies of Electric Machines

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AMONG the methods available for determining losses and efficiencies of electric machines, many have been developed to their utmost capabilities. Some measure individual losses separately and with good accuracy,[1–10] but many have inherent drawbacks. There still remains the desirability of a satisfactory over-all test which will rate the performance of all sizes of machinery under actual conditions of loading and yet yield highly accurate results. This aim is largely met by the calorimetric method and is justified by its basic character. A simplification of the classical testing technique can be introduced in which the important measurements are all electrical. By this means, accurate measurements of heat values are obviated, and the calorimeter becomes in reality only a comparator for a substitution process. Test results of calorimetric measurements on small induction and d-c motors show excellent agreement with other tests on the same machines. Analysis of the method indicates that it is equally applicable to all classes of machines of any size and that it can be used more generally for over-all loss and efficiency measurements under loaded conditions.

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Calorimetric Method

The classical form of the calorimetric method makes use of the basic principle that all losses are converted into heat. Measurements of thermal values from specific heat of the coolant, its volume, and temperature rise, have involved uncertainties and difficulties which have tended to discredit the method. However, these can be avoided by a simplification possible with electric machinery, in which the volume of the cooling medium is kept constant, and the temperature rise is measured under load and again with calibrating power input, thus giving a direct indication of the total losses within the system. The method is applicable to many types of machines, motor or generator, d-c or a-c, with only slight revisions in detail for each. Since total losses may be determined by it, and all other losses can be measured separately, stray load loss can be determined by this type of test. The American Standards Association Standards[8] and the test codes recognize the use of the calorimetric method, although reports of its use are rather scarce in the American literature. European tests reported to the International Electrotechnical Commission meetings at Prague in 1934 and Scheveningen in 1935 by the late Edouard Roth[2,11] show the development of simplifications in the calorimetric method. The procedure was to enclose the machine under test in a trunking or duct, through which a constant quantity of air was blown. Temperature rises from inlet to outlet were determined by the resistance of grids in the air stream, and losses to produce equal rises were introduced by means of heaters. Elaborate interpolation of data was required, because of difficulty in adjusting to exact equality.

Further extension of these simplifications is made in the tests being reported in this paper. Where possible, the calibrating losses are introduced directly into the machine under test so as to simulate actual losses exactly. A succession of points is taken so that a graphical determination of equal temperature-rise conditions may be made between the loaded and calibrating runs, thus minimizing the work of calibration. The use of differential thermocouples makes possible direct readings on a microammeter without the necessity of delicate potentiometer readings.

Equipment and Procedure

The test work was done on two machines, one an induction motor and the other a d-c motor of comparable size. The equipment consists principally of:

A double-walled calorimetric box of heat-insulating material to enclose the machine under test, in this case reversible so as to be usable on either end of the set.

A constant-speed blower to force air through the box.

Thermocouples differentially connected between inlet and outlet ports.

Power-measuring instruments in the leads to the test box.

Heaters inside the box near the test machine.

Figures 1 and 2 show the arrangement of parts.

The pair of machines is mounted on a wooden base with an iron subbase, and bolts are staggered so as to prevent any direct path of high thermal conductivity to the outside. A layer of insulating board is fitted around the machine feet, and the box is sealed to it. A slot on one side makes provision for a close-fitting air seal around the shaft, the coupling being outside of the enclosure. Dead air spaces

mismatch at the load end will affect the input impedance of the line, and in any practical case the coupling between the line and the generator will have to be adjusted to give the best power transfer.

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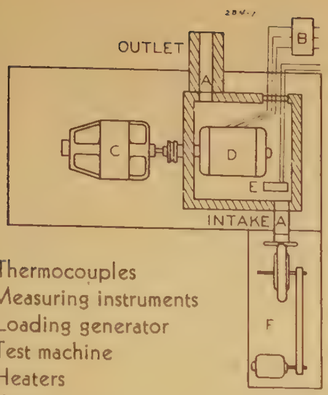

A—Thermocouples
B—Measuring instruments
C—Loading generator
D—Test machine
E—Heaters
F—Blower unit

Figure 1. Arrangement of calorimeter and machines

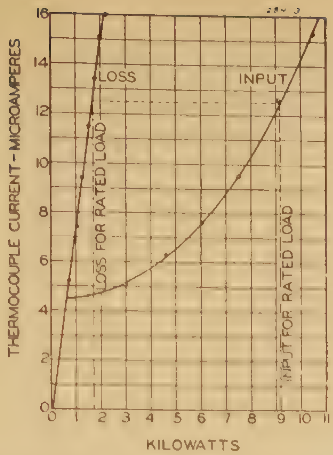

Figure 3. Temperature differences under load and calibrating conditions

Value of loss for corresponding input indicated by equal temperature differences

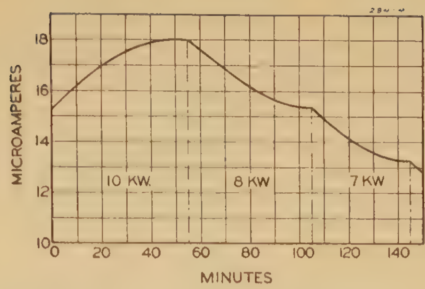

Figure 4. Typical temperature-difference curves for three values of total input to box with machine loaded

Readings taken at end of intervals

are filled with rock wool. A terminal plate provides electrical connections to the inside. Thermocouples are connected differentially in series between the blower pipe at the inlet and an insulated outlet at the diagonally opposite corner. Small ports are used to insure thorough mixing of the air, thus keeping all parts at a uniform temperature. This was verified by test and by observing the air stream through a window at the top of the box when smoke was injected.

As indicated in the introductory discussion, this method uses the calorimetric quantities for a comparison of the power values which will produce the same temperature conditions:

1. The machine is operated under load, and electric power input and thermocouple measurements are made. All losses of the machine, whether in electrical or mechanical form, are dissipated within this box and eventually contribute to the heating of the air. No measurement of shaft output is made unless desired as a check.

2. The coupling between machines is broken, and a set of measurements is made with the machine running idle to find the power input that is required to duplicate previous thermal conditions.

Finding the loss input which gives exactly the previous (loaded) temperature difference is unnecessarily tedious. For satisfactory results and minimum time used on a test, the better procedure is to take two curves of temperature difference (microamperes) for several values of heating, the air volume being held constant. One curve is taken as a function of total input under loaded conditions, and another is taken as a function of total losses. Such a set of curves is shown in Figure 3, with both curves plotted to the same scale for illustrative purposes. The total losses for any input are then determined from the curve of calibrating loss at the corresponding temperature difference. The machine output is found by subtraction and efficiency is computed in the usual manner.

As may be expected, time must be allowed for the thermocouple currents to become stabilized. Figure 4 shows typical curves for one run (air rate different from Figure 3). In the case of this thermal system, about one hour was required per reading, although this time could be shortened by changing the air rate during the transition period, gradually returning to the standard value. The air rate must be selected so that a total difference between inlet and outlet temperatures does not exceed about 20 degrees centigrade to approximate nominal ambient conditions. Since actual thermal values are not required, blower speed need be kept constant only during any one set of measurements. Air density and humidity should remain constant within the same period or not change appreciably. Inlet air temperature may vary slightly without error, but care should be taken to keep loading and control rheostats reasonably distant from the inlet. Because the method requires only repeatable conditions, even slight air leaks are not serious so long as they stay constant. Speed of the machine under test may vary slightly without serious error, due to changing the ventilation pattern within the calorimeter.

Up to this point, the procedure is perfectly general for any type of apparatus. The loaded temperature determination is likewise general, but consideration must now be given to methods of introducing the no-load or calibrating power into the calorimeter. The test machine is disconnected from the load, and it is allowed to run at its nominal speed. Additional heat may be introduced by heaters so placed that the internal ventilation will circulate the heated air. The calibrating power is thus the sum of the no-load input to the machine and the power to the heaters. In the case of induction machines, the entire power may be introduced into the machine by raising the applied voltage so that increased running-light losses will duplicate those at full load. This has the advantage of placing all the loss within the machine, although current

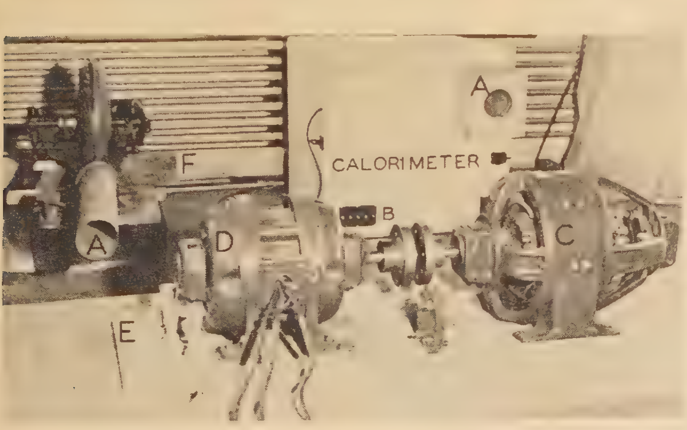

Figure 2. Calorimeter and machines

A—Thermocouples
B—Terminals for input and instruments
C—Loading generator
D—Test machine
E—Heaters
F—Blower unit

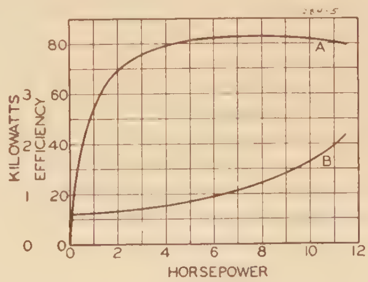

Figure 5. Total losses and efficiency at different loads derived from Figure 3

A—Per cent efficiency *B*—Total losses

limitations may require partial use of the heaters. Synchronous machines permit similar abnormal adjustments by field control of reactive currents, but it must not be overlooked that field copper loss contributes to the total heat within the calorimeter. D-c machines are the least amenable to this treatment, and heaters have to suffice.

Test on Induction Motor

An induction motor which had been thoroughly calibrated by many tests was available for test by the calorimetric method. This was fortunate, because the induction motor illustrates the good features of this test to the utmost, and illustrates the *method* well. In the calibration of the box, all of the necessary no-load losses can be confined to the machine itself by raising the applied voltage and exciting current until copper and rotational losses are of the order of total loaded losses, provided the stator current is not dangerously large. This permits the nearest approach to the ideal of having all losses identical in the two tests. The squirrel-cage machine used is rated 10 horsepower, 220/440 volts, 26.6/13.3 amperes, 60 cycles, 1,710 rpm squirrel-cage motor, and was operated at 220 volts.

Complete data were obtained from which separation of losses could be made, the calorimetric tests providing only the total loss value. Slip, stator resistance, and rotation losses were measured in addition to line current, voltage, and power usually measured in the efficiency test. Thermocouple current is the indication of temperature difference and is plotted in Figure 3 for the steady-state deflections given by a certain total input in the load run, and by a certain total loss input in the unloaded run. Since corresponding deflection currents indicate corresponding power values, the total loss for a given input is found by following across the proper ordinate to pick off the loss abscissa. The dotted lines indicate test data for rated load on the machine. It may be seen that deflection is proportional to loss in the box for constant blower speed. This is to be expected so long as the heat is dissipated in the same manner at all loads. As the box gets hotter however, other factors appear to permit heat to escape by conduction, with a resultant flattening of the curve. These make an actual calibration for each test very essential. An additional test using heaters for no-load input was made with identical results.

The final curves of total losses and efficiency derived from the data of Figure 3 are shown in Figure 5, with output as the abscissa. The full-load efficiency of this machine is 81.5 per cent as compared with 81.7 per cent from other test methods using conventional losses plus stray load loss measured separately.

As a demonstration of the accuracy possible with the calorimetric method, a set of values of stray load loss found by this test is plotted in Figure 6. The experimental points are obtained by two groups of observers. The solid curve is the stray load loss as determined by other methods,[4,5] several curves not widely different being averaged together. These results are quite consistent, especially when it is appreciated that stray load loss here is found as an indirect measurement by subtracting all other known losses from the total determined by the calorimetric method.

Test on D-C Machine

The calorimetric set used was reversible; so the box was turned around on the base, and measurements were made with the d-c machine as the test machine. The induction motor acting as a generator was loaded by operating it from a separate alternator. In this test, the calibrating losses were supplied by heaters in addition to the running-light input. The full-load efficiency by this method was 83.1 per cent, whereas the efficiency by conventional methods plus stray load loss[10] was 83.4 per cent.

Conclusions

The calorimetric method described in this paper consists of two fundamental steps only:

1. Determination of a temperature rise of the cooling medium under loaded conditions.

2. Substitution of an equivalent measurable loss which will produce the same temperature rise under no-load conditions.

This is a comparison or substitution method which determines with high accuracy the total losses of a machine under conditions of actual loading. The method permits good comparative data to be taken even under poor calorimetric conditions, although refinements improve the ease of obtaining good results.

Losses measured by it compare directly and favorably with those of other methods of high accuracy and show a final stray load-loss value very close to those from other tests. Although tests of only two machines are reported, the

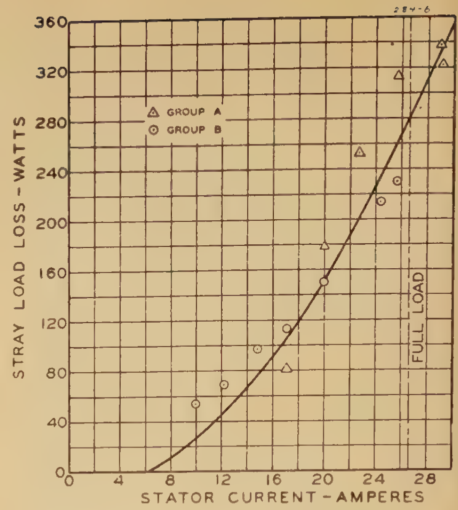

Figure 6. Stray load loss of induction machine tested

Solid curve average of tests by other methods.[5] Experimental points determined by calorimetric method by two groups of observers

method has been demonstrated to be workable and fundamentally sound. Since it depends on a basic principle, the accuracy which may be expected will be determined by the care and techniques employed. Further attention to the calorimetric method will doubtless bring forth other advances in its use.

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High-Voltage Fusing of Transformer Banks

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Synopsis: The paper summarizes some 15 years' experience with the high-side fusing of 11-, 22-, and some 66-kv transformer banks. The original purpose of fusing has through this experience been expanded to cover functions that are deemed desirable, and the authors feel that their approach to the specific protection field served by fuses has resulted in satisfactory protection at minimum cost.

The results of some faults occurring in fused and unfused banks are described briefly, and the various factors taken into consideration in selecting the proper fuse to use are discussed. The conclusions drawn are that fuses, when applicable, are a satisfactory economical means of obtaining reasonable coverage for faults in and beyond transformer banks.

THE experience of the Duquesne Light Company with what we shall call modern high-voltage power fuses began in 1926 when Schweitzer and Conrad liquid fuses were applied to the high-voltage side of several 22-kv banks in stations serving customers from a radial transmission line. The previous outages to this particular line were the determining factor in these fuse applications as an effort to improve service to the remainder of the line in cases of station trouble.

Early Experience

For the first few years our experience with these fuse installations was anything but satisfactory. These fuses deteriorated, causing uncalled for operations, and many justified blowings resulted in failures to clear, flashover of inadequate spacings, and flashover of inadequate insulators. These fuse operations were carefully followed, and such conditions of installation as contributed to unsatisfactory performance were eliminated. Mountings were replaced with higher insulation, spacing and clearances generally increased, and 34.5-kv fuses standardized upon for our 22-kv service. These changes, together with the manufacturers' changes of liquid, cap design, fuse element changes, and other advances, had made it possible to place considerable confidence in high-voltage fuses such that by 1937 our experience justified an extension of our fusing applications and the development of a general policy of fusing where applicable.

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Experience up to 1937—Fused Versus Unfused Stations

By 1937 our number of fused banks had increased in proportion to our growing confidence in high-voltage fuses, and at that time we had 32 power applications in both customer and company distribution stations. An analysis of performance of fused and unfused stations for the years 1930 to 1937, as disclosed by fault records, brings out a striking comparison, and the records are tabulated in Table I for parallel consideration.

In the fused group we had had no fires, no explosions, no serious damage and, in many cases, immediate return to service of the faulted equipment, since prompt clearing had prevented permanent faults. Contrasted with this in the unfused group we had had three fires, one explosion, and had lost nineteen transformers such that they were either scrapped or completely rewound.

Origin of High-Voltage Fusing Policy

The original purpose of the high-side fusing of transformer banks was to minimize outages to multicustomer circuits where one customer fault would otherwise involve a total line outage.

An analysis of the above performance record disclosed that the fuses, in performing this original function, had provided additional benefits by preventing fires, explosions, or serious damage.

Based on this analysis, a policy has been established to provide adequate protection for all new bank installations and, over a period of years, to cover existing stations using fuses wherever applicable.

Experience 1937 to the Present

Our fuse installations now number well over 126, cover banks ranging in size from 50 kva at 22 kv, up to 10,000 kva at 66 kv, and involve the use of Schweitzer and Conrad types *D*, *C*, and *SM*, and Westinghouse type *BA*.

From 1937 to the present we have had 59 faults involving 87 fuse operations all functionally correct, and several of these,

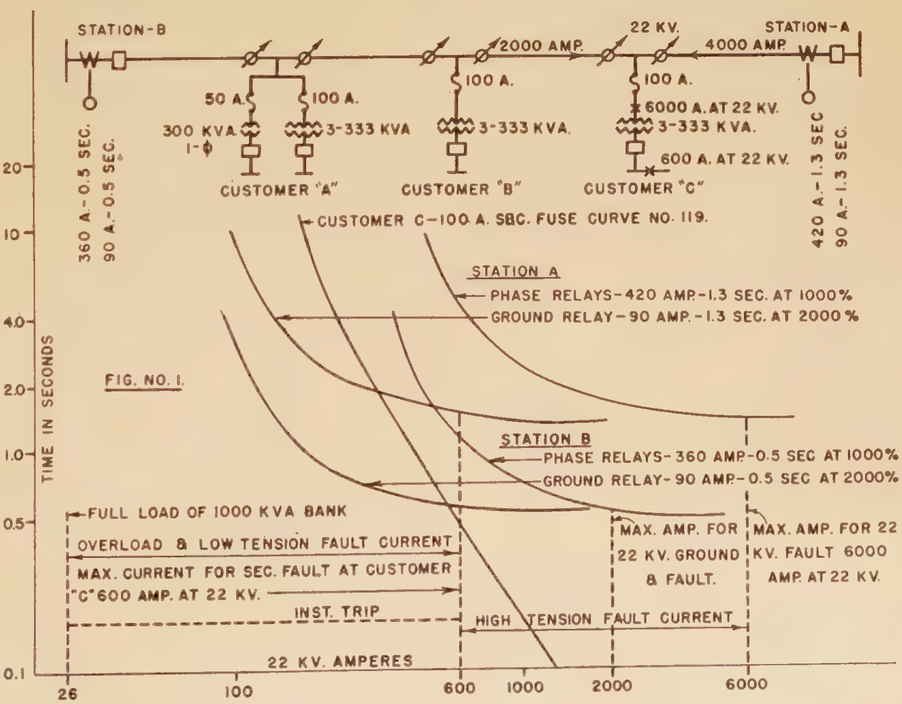

Figure 1. Selection curves of transmission-line relays, transformer-bank fuses, and low-voltage protection

we feel, have avoided possible explosions and fires, and in addition, have prevented interruptions to other customers served from the lines involved. Many of these fuse operations have cleared low-voltage faults, where customer protection has been inadequate or inoperative, and where such faults could not have drawn sufficient current to operate the high-voltage protective relays until after winding breakdown. Such fuse operations have avoided transformer-winding burnout.

In one station fuses were used on each of four 10,000-kva, 66/11-kv transformer banks to avoid the installation of a high-voltage breaker and relaying, and, in final analysis, to give the customer better service. On two occasions these fuses have performed as intended, once by blowing to clear a fault not covered by the customer's low-voltage protection, and once by not blowing on a similar type of fault which was cleared by such protection. In the first instance high-voltage breaker protection would have given a complete interruption until performance of switching to segregate the defective low-voltage equipment, while the fuse application caused no interruption to three fourths of the service and segregated the fault promptly and properly.

The success we have had with our fuse installations has not been due alone to the improvement in the fuses themselves, nor to better installation conditions, but has been the result of these factors together with a judicious selection of the proper fuse for the existing conditions, and to a rigid inspection policy, and the practice of removing such fuses that give any indication of suspicion as to their condition.

Control of Fuse Applications (Design Stage)

In deciding all protective layouts, our design engineers seek the advice and recommendations of the protection engineer, and whether the problem involves that of the more complicated types of reactance relays, pilot-wire schemes, differential protection, or fuses, the procedure is the same. In following this policy, the simplest fuse installation gets the necessary careful consideration it requires, and all of the factors entering into proper selection get uniform attention.

Our protection policy, based on experience, is that all portions of an electrical system should be covered by protection of some sort to clear whatever fault may develop. This policy cannot always be met on an economically justified basis by relays and breakers. Where this condition is encountered, high-voltage fuses frequently provide a reasonably satisfactory solution.

Selection of Fuses for Transformer-Bank Protection

In selecting fuses for transformer-bank protection, investigation is made of the fault current obtained for faults, both on the high-voltage and low-voltage side of the transformer bank, and consideration is given to the protection that is provided on the low-voltage side of the transformer bank. A fuse is then selected with a time-current curve and a current-carrying capacity that will provide the desired time of operation for low-voltage faults.

In many cases, transformers that are fused supply industrial customers, and a variety of types of protection are used by the customer on his own breakers. Settings of these protective devices are determined, and high-voltage bank fuses are chosen to select with the low-voltage equipment. In some cases where customers' equipment is involved, it has been found that the protective equipment has been set unnecessarily high in current and time, and these settings have been lowered to the mutual advantage of the utility and the customer.

The fuse-current rating in relationship to full-load current of the protected transformers is not selected as any arbitrary value in relationship to the size of the transformer bank, but is selected as required with due consideration of the time-current characteristics of the different fuses available.

Since the application of these fuses is for the clearing of faults in and beyond the transformers, no attempt is made to protect the transformers against overloads. In this connection the current rating of fuses on our system varies from $1^1/_2$ to 4 times the bank full-load rating. The time delay and current-carrying capacity of the fuses are inseparable and are varied in order to obtain the necessary time delay for clearing of low-voltage faults. In practice the time delay provided for low-side faults varies from a minimum of 0.3 second on some lighting transformers to a maximum of 1.5 seconds on some power banks.

Time Selection of Fuses With High-Voltage Relays

While fuses are installed primarily to obtain clearing of faults in transformer-

Table I. Comparison of Performance of Fused and Unfused Substations

Fused Stations	Unfused Stations
1. Two fuses operated to clear a defective transformer caused by water entering the winding through a leaky low-voltage bushing.	1. Entire station, consisting of three 2/1 transformers and housing, destroyed by fire as result of transformer failure. Evidence of original failure lost.
2. All fuses operated to clear a defective bank of three 667-kva units during flood.	
3. One fuse operated to clear a failure of customer static capacitor on 2,300-volt side of bank.	2. Three 333-kva transformers destroyed, fortunately without fire, when a customer low-voltage fault held on, the customer's breaker found inoperative.
4. On two different occasions one fuse operated to clear a defective nonautomatic breaker bushing which had flashed over during storms.	
5. Three fuses operated to clear a high-voltage bus flashover.	3. Three 100-kva units destroyed, necessitating complete replacement as result of low-voltage winding failure.
6. Two fuses operated on initial cut-in to clear a low-voltage potential transformer which had been connected reversed with 115-volt winding on the customer's 2,300-volt bus.	4. Three transformers destroyed as result of low-voltage customer-equipment failure. Fault held on until breakdown of high-voltage winding, when 22-kv protective relays operated. Fortunately no fire.
7. On three different occasions fuses operated to clear trouble on customer-owned high-voltage transformers.	
8. Two fuses operated to clear a 2,300-volt bus fault caused by boys having thrown wire across a bare low-voltage bus.	5. Failure of one transformer resulted in oil fire which destroyed the entire station on customer premises.
9. One fuse operated to clear an internal flashover of high-voltage lead in one transformer.	
10. One fuse operated to clear defective low-voltage customer breaker and again three fuses operated to clear a flooded three-phase unit, leaving line in service.	6. Transformer failed and exploded at the station, resulting in fire which involved three other units and loss of all electrical equipment. Several intermittent grounds had been indicated at the supply station.
11. On four different occasions fuses operated to clear internal flashovers in transformers during storms.	
12. On three occasions fuses operated to clear faults on four-kilovolt circuit fed from bank. Low-voltage breaker inoperative.	7. Low-voltage disconnects pulled under load, fault held on until winding breakdown. Three 500-kva units complete winding loss.
13. One fuse operated to promptly clear an accidental contact with a ground chain being handled with rubber gloves.	8. On two occasions customer low-voltage cables caused failure of low-voltage bushings, due to fault hanging on and ultimately communicating to the high-voltage side of the bank.

bank windings or on secondary busses that would not otherwise be cleared properly, other benefits are sometimes obtained. These are:

(*a*). To prevent or decrease outages to other customers.

(*b*). To reduce damage to high-voltage bushings of transformers due to quick clearing.

In connection with item *a*, selection is not always obtained between the transformer fuses and the protective relays on the transmission system. In many cases, however, selection is obtained, particularly for short-circuit faults, and there are many instances where long outages to other tap or nonautomatic loop stations have been prevented by fuses clearing transformer faults before relays operated.

In some instances, the fuse and transmission-line relays may operate together, in which case the transmission line can be returned to service, as the fuses will have cleared the fault.

On many faults, however, the transmission-line ground relays do not select with fuses during line-to-ground faults, if the fault currents are of a low value. This nonselection is recognized and accepted.

With regard to item *b*, many transformer-bank high-voltage bushing flash-overs have been cleared so quickly that service could be restored immediately.

Illustration of Specific Application

In Figure 1 is shown one example of application of fuses to three relatively small customers, supplied with tap service from a 22-kv loop circuit. In this particular case the distance between automatic stations is only about $1^1/_2$ miles and is close to a large 66/22-kv substation; as a result the current for 22-kv faults is relatively high.

Time-current curves of phase and ground relays at stations *A* and *B* and the melting time of fuses at customer *C* are shown. Maximum current of 6,000 amperes at 22 kv for a fault on the high-voltage side of transformer at customer *C* is indicated and a current of 600 amperes at 22 kv for fault on the low-voltage side of the same transformer.

These curves show that for a fault on the low-voltage side of the transformer at customer *C*, the melting time of a certain 100-ampere fuse is 0.45 second. This is adequate time to permit the low-voltage bank oil circuit breaker, with instantaneous trip coils, to operate. The curves also show that the phase relays at stations *A* and *B* select with this fuse at all current values. It is shown that the ground relays at the two stations do not select with the fuses at all currents. However, as the fault current is supplied from both ends of this circuit, the fuse obtains more current than either of the ground relays and may select. Also for any ground fault current above 1,000 amperes, the fuse will properly select with the ground relays. As it is possible to obtain 2,000 amperes ground current at this location, the probability of the ground relays selecting with the fuses is very good.

From the above it is seen that in this case the fuses not only serve to protect the transformer banks, but also to protect service to the adjacent customers.

The above illustration is only one of numerous similar conditions on our system.

Miscellaneous Factors to Be Considered in Applying Fuses

In applying fuses for the protection of transformers that supply various types of loads, different factors must be considered, depending on the type of load.

For example, fuses on the high-voltage side of a furnace transformer should have a continuous carrying capacity above the value of current that can be obtained for the condition of shorted electrodes in the furnace, as this is a condition that may exist for a considerable period of time during normal operation of the furnace. In this case, the fuses do not adequately protect the transformer bank in event of low-side failures, but do serve to disconnect the equipment from the system in event of severe faults in the furnace transformer.

In applying fuses for the protection of a transformer bank that supplies distribution circuits that are protected with oil circuit breakers, care must be taken to obtain sufficient time selection between the breaker opening and the blowing of fuse, so that the fuse will not operate incorrectly on normal reclosure of the oil circuit breaker on a faulted distribution circuit. Our present practice is to allow from 0.7 to 1.0 second selection between low-voltage reclosing feeder relaying and high-side bank fuses and while we have experienced no difficulty with this difference in selection, we realize it is open to question and to reconsideration as experience requires.

Conclusion

It is not intended to convey the impression that high-voltage fuses should be used for the protection of transformer banks in preference to relays and oil circuit breakers. Where this equipment is available or can be justified, it should be used. In many cases, the cost of oil circuit breakers cannot be justified, and in such cases, fuses, if applicable, can be used.

In general, it is felt that, in the voltage range of our experience, the modern high-voltage fuse, properly applied and not forgotten, is a reliable protective device that should be added to the other protective schemes, and that it has a definite place in meeting requirements at moderate cost that cannot otherwise be economically justified.

A 600-Volt Enclosed Limiter for Network Use

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Synopsis: The extension of the conventional network system to industrial plants has established requirements for a higher-voltage limiter.

The network system has been adopted as a standard by many utilities for 120–208-volt distribution, whereas industrial-plant distribution is usually at higher voltages up to 600 volts due to the predominance of power load. The requirements of a limiter for this service are more severe, due to the proximity of operating personnel and increased interrupting duty associated with higher voltages and shorter cable runs.

A new totally enclosed limiter has been developed for this application. This limiter embodies new principles of operation and will interrupt fault currents in excess of 50,000 amperes at 600 volts without perceptible noise or visible demonstration.

NETWORK systems as originally conceived and installed did not make use of limiters, for the systems were based on the premise that cable-insulation failures encountered on 120–208-volt circuits would be self-clearing. Experience has proved this premise to be generally correct.[1] Obviously, however, there can be solid metal-to-metal faults that are not self-clearing. Furthermore, faults, although self-clearing, could persist until heating of the cable resulted in permanent damage to the cable insulation. Usually, such persistent faults would spread to adjacent cables, materially increasing the resultant damage. The large amount of power liberated in these extensive faults often produced combustible gases in sufficient quantities to cause manhole explosions.[2] These factors led to the development of the limiter which consists of a fusible section located at the ends of cable runs to guarantee the clearing of any fault on the cable before its insulation is damaged or the fault spreads to other cables. The correct analysis and solution of this problem is due largely to the work of C. P. Xenis of the Consolidated Edison Company of New York.[3]

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Limiters have been in service on 120–208-volt network systems for several years and have been found ideal for the application.[4] Factors contributing to the rapid adoption of limiters on network systems are as follows:

1. The limiter requires no maintenance. It has no contacts or adjustments and consists of a simple conductor having swedged or bolted connections. It can be connected into the cable and forgotten. The rapid adoption of the limiter has been due principally to this one factor of simplicity and reliability.

2. It eliminates damage to unfaulted portions of the cable. Being a thermal device, it is ideally suited to the protection of cable insulation from heating due to fault currents.[5]

3. The limiter insures the rapid clearing of heavy fault currents, thereby minimizing system disturbances and localizing all faults.

4. In networks having two or more cables per phase, proper selectivity between limiters is automatic. Co-ordination with network protector fuses can be easily obtained by proper planning.[6,7]

5. A quick reclosing device in place of the limiter would be of no particular benefit, for considerable maintenance must be performed on the cable after a fault.

6. The limiter is relatively inexpensive and renders a service out of all proportion to its cost. It may be used at both ends of all cables. Consequently, a fault will disable only a short section of cable.

Previous Limiters

Until the advent of industrial networks the application of limiters was confined almost exclusively to 120–208-volt networks for city distribution. The re-

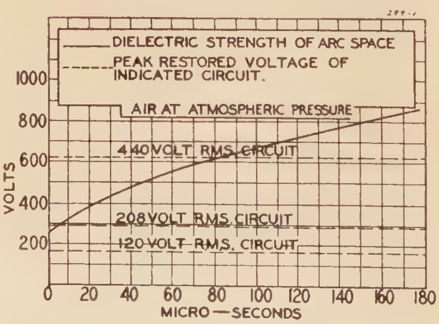


Figure 1. Recovery of dielectric strength of arc space of short arcs with reference to the peak restored voltage of standard systems

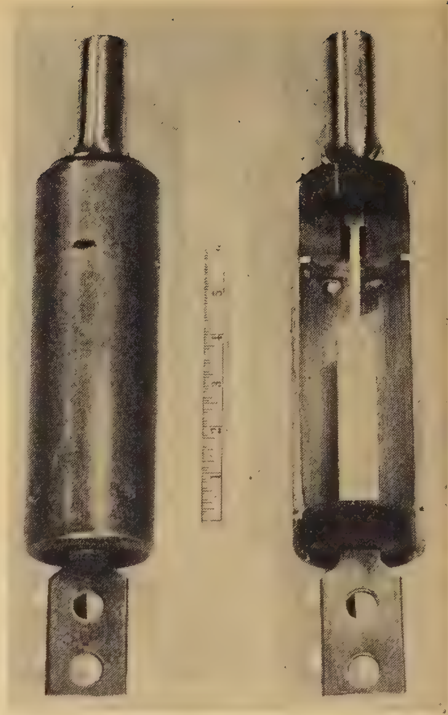


Figure 2. Assembled and sectional view of new limiter

quirements of limiters connected in these systems were not too severe. The low system voltage combined with short-circuit currents which seldom reached 30,000 amperes represented moderate interruption duty. Since the limiters were usually placed in underground vaults, there were no strict requirements on noise or demonstration. In addition, the limiters were not the only line of defense, for most faults at this low voltage are self-clearing.

The limiter developed for this service consists of a reduced metallic or fusible section incorporated in a connector lug

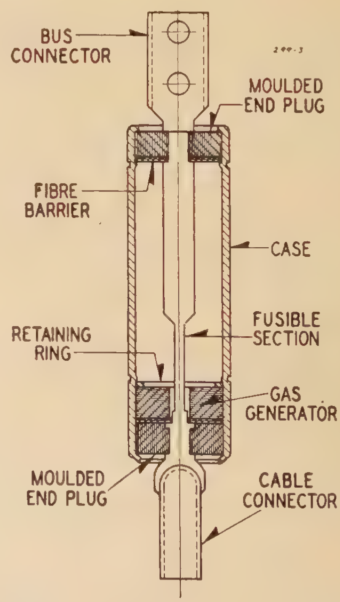


Figure 3. Cross section of new limiter

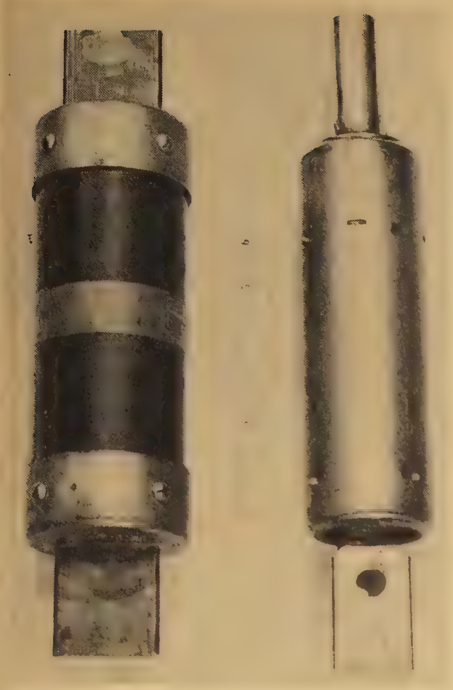


Figure 4 (left). Comparison of new limiter with cartridge fuse

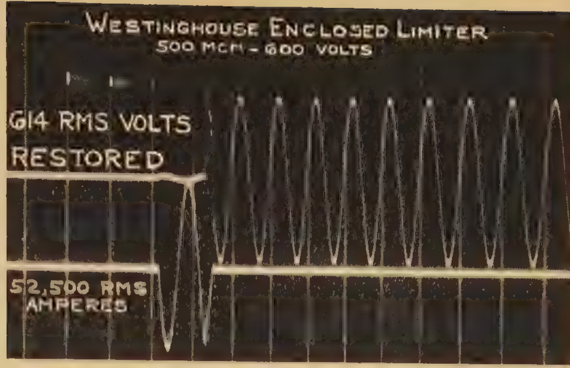


Figure 6 (right). Interruption of current above rating

or other connecting device. The fusible section is surrounded with heat-resistant insulation, and the whole assembly is placed in a rubber sleeve and securely taped to prevent the escape of hot gases or flame during interruption. These limiters, when operating within their current and voltage limits, have given successful interruptions without demonstration. They have been found admirably suited to the service for which they were designed.

Requirements for a New Limiter

The entrance of the network distribution system into the industrial field created several new problems. Systems up to 600 volts were encountered, cable runs were considerably shorter, and power concentrations were greater. All of this led to greatly increased short-circuit currents which had to be interrupted at these higher voltages. Under these conditions, faults are seldom self-clearing.[8] Referring to Figure 1, it is seen that an arc space recovers a dielectric strength of about 250 volts instantly at current zero. This is sufficient to cause interruption of arcs on a 120–208-volt circuit. Considerable time is required for the arc space to recover sufficient dielectric strength to cause circuit interruption at higher voltages; hence, faults on a 440-volt system must be cleared by an interrupting device. Limiters, therefore, were necessary to guarantee the interruption of the circuit. Strict requirements concerning noise and demonstration were imposed on the limiters, because they were required to operate in close proximity to factory personnel. Furthermore, the limiters for industrial application could not present any fire hazard whatsoever.

In accordance with these requirements, it was decided that a limiter for this new service should have the following characteristics:

1. It must be totally enclosed and have minimum space requirements.
2. It must have an interrupting rating of 50,000 amperes at 600 volts, 60 cycles.
3. It must have a low temperature rise to permit bunching of limiters in confined spaces.
4. It must have a time-current characteristic to permit co-ordination with other limiters, co-ordination with its cable insulation, and co-ordination with the network protector fuses.
5. It must have suitable terminals to permit coupling with the proper cable.

Design of a 600-Volt Limiter

The design of a totally enclosed limiter of the required small size presented the dilemma usually associated with engineering problems. On one hand the device must have five times the interrupting ability of a cartridge fuse of comparable current rating, and on the other hand, not only must it be made smaller, but the demonstration attendant to circuit interruption must be eliminated.

The design was accomplished by solving two problems:

1. The metallic vapors and gases from the heavy fusible element had to be accommodated. Tests revealed that a copper sheath or cover around the fusible element would supply a condensing surface on which the fused metal would be deposited, thus eliminating high internal pressures.
2. Totally enclosed interrupting means had to be provided to insure positive interruption of all currents up to the interrupting rating. Further development evolved a concentric fiber gas generator, which would supply the necessary turbulence to cause deionization of the arc.

Figures 2 and 3 show the final design of the limiter. The conducting element is made from copper tubing which is

Figure 5. Interruption of moderate current

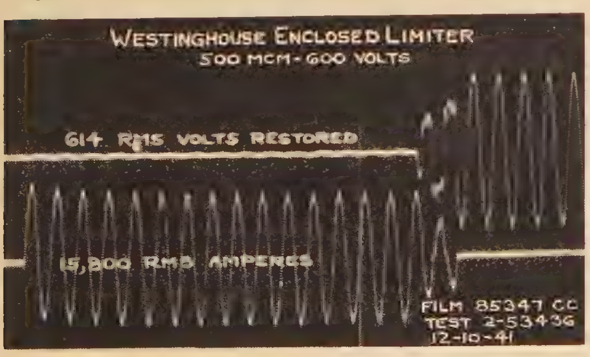
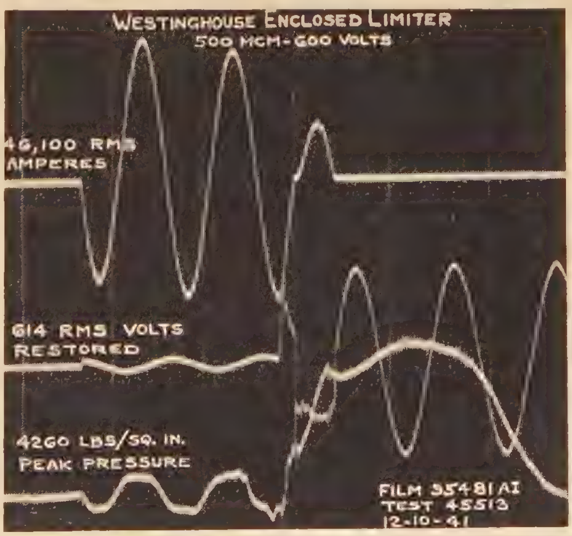


Figure 7 (right). Pressure record during interruption by new limiter of fault current near rating

The pressure variations preceding the melting of the fusible section are induced but are not present after the current is interrupted



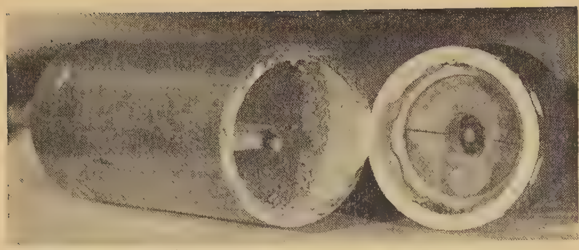


Figure 8. View of limiter cut open after interrupting moderate current

Note deposition of copper on inside surface of case

flattened and sized to produce the proper fusible element and terminal connectors. The closure plugs are moulded on the conducting element in an extrusion-type mould with the retaining ring in place. Split fiber washers are used as barriers to prevent arc flame from burning the moulded material and to produce the gas generating structure. This structure is held from moving under the stress of interruption by the retaining ring which fits up against embossed shoulders on the outside casing. A copper tube forms the case of the limiter and in addition furnishes the condensing surface for the metal vapors. The conducting element is restrained from sliding by embossing the copper tubing, after which the entire assembly is sealed by swedging in the ends of the case. Figure 4 compares the size of the limiter with a 600-volt cartridge fuse of the same current-carrying ability.

Tests

Repeated interruption tests were made throughout the entire current range. In many tests, the limiter was completely surrounded by surgical cotton in accordance with the procedure established by the Underwriters' Laboratories, Inc. In no test was the cotton discolored, thus proving the absence of flame or hot gases external to the limiter. Figure 5 is an oscillogram showing an interruption at a moderate current value. In Figure 6 the interruption of a current in excess of the rating of the limiter is shown. Note the shift in power factor of the circuit caused by the relatively high arc voltage. This characteristic of the limiter is an advantage in that it aids in the interruption of the circuit and protects the system from voltage stresses. Internal pressure is recorded on the oscillogram in Figure 7. The short duration of the pressure is caused by the rapid condensation of the metallic vapor by the copper case and by the cooling of the generated gas. The condensed metal vapor is seen in Figure 8 as irregularities on the inside surface of the blown limiter. Voltage tests were made on blown limiters by putting them on overvoltage immediately after interruption. In all cases the limiters withstood voltages above 2,200 volts continuously for several days until removed from test.

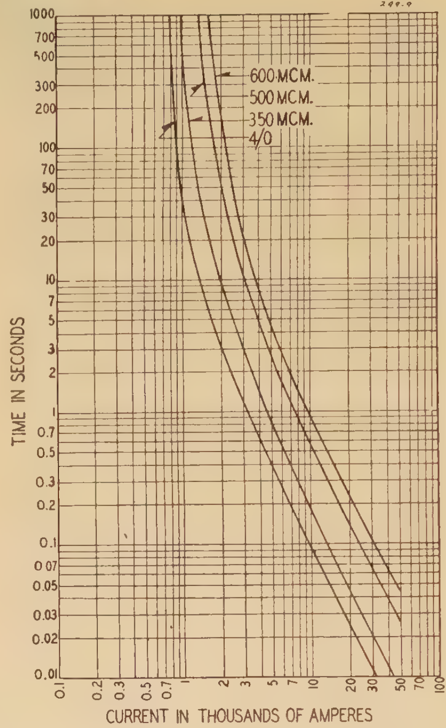


Figure 9. Time–current curves for new limiter

The limiter is designated by the size of cable for which it is intended

The sizes of fusible elements were determined by the co-ordination requirements. Time-current curves for several common sizes are shown in Figure 9. A study of the curves will reveal that any one limiter will be blown without damage to the other limiters on the system, if the fault current is fed to the limiter which interrupts the circuit through at least two other limiters in parallel. This is the case in a properly designed network system, where there are two or more cables per phase, as will be seen by reference to Figure 10, a schematic diagram of a typical load bus unit.

Temperature-rise tests were made with the limiter connected to the supply by eight-foot lengths of the proper size type-*R* cable. The limiter was supported in free air and was shielded from air cur-

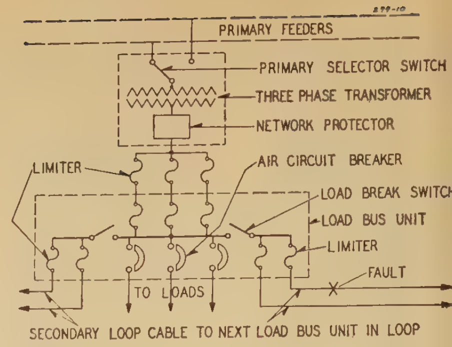


Figure 10. Unit of network system in which selectivity between limiters is automatic

Note that current flows in parallel through all other limiters on the bus to the limiter clearing the fault

rents by a canvas screen placed completely around the device. Temperature rises were found to be less than 30 degrees centigrade for all sizes at the maximum current ratings of the cable.

Conclusions

Past experience has shown the desirability of a network system and the effectiveness of limiters in providing the proper protection. The new 600-volt limiter described in this paper is serving in a number of industrial plants at the present time, and all experience has been satisfactory. In addition, extensive laboratory testing has adequately proved the new limiter's ability to perform its functions. There has been provided a new 600-volt limiter, small in size, but capable of interrupting large currents without perceptible noise or visible demonstration.

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TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Any discussion of these papers will appear in the December 1942 Supplement to Electrical Engineering—Transactions Section

Modern Impulse Generators for Testing Lightning Arresters

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NATURAL lightning is very destructive and so elusive that it cannot readily be used for testing. However, the behavior of materials or equipment which are subject to lightning may be studied with artificial lightning from the impulse generator. In impulse testing of insulation strength, it is necessary to control only the wave shape and magnitude of the impulse voltage. There are numerous papers[1–5] describing methods of producing and measuring impulse voltages, and other papers[1,2,6–9] show how to calculate the circuit constants required to produce such waves.

The complete testing of lightning arresters requires, in addition to means for obtaining voltage waves, facilities for the accurate control of both magnitude and wave shape of discharge current. A variety of these impulse current waves are needed to simulate service conditions reasonably. The newly revised AIEE Lightning Arrester Standards No. 28 include tests with 10x20-microsecond waves of 5,000, 10,000, and 20,000 amperes; 5x10-microsecond waves of 65,000 and 100,000 amperes; and 10,000, 15,000, and 20,000 ampere tests with a nominal wave steepness of 5,000 amperes per microsecond. Since the internal impedance of the arrester is a factor affecting the impulse current, the complete testing of various types and ratings of arresters requires impulse generators with considerable flexibility and facility for quick adjustment. This paper describes several impulse generators designed primarily for lightning-arrester testing, and which have proved over years of constant use to have the desirable features of safety, flexibility, reliability, and convenience.

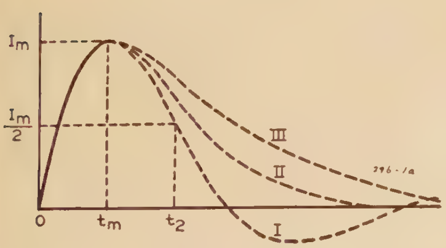

Figure 1a. Typical impulse-current waves

The time to crest for all three waves is designated by t_m, but t_2, as shown, represents the time to half value for wave I only

The circuit constants necessary to produce various current waves cannot be calculated exactly, because the resistance of the arrester valve element is not constant but a function of the voltage or current. So far as the author is aware, the methods for determining such circuit constants have not been published although there have been described impulse generators[10,11] which are adapted for lightning-arrester testing with different current waves. Through long experience, methods of approximate calculation have been developed, chiefly by the use of curves, which enable circuit constants to be selected roughly with a minimum of cut-and-try procedure. These methods are general in character and are applicable to unidirectional or oscillatory waves, including, of course, those specified in the AIEE Standards.

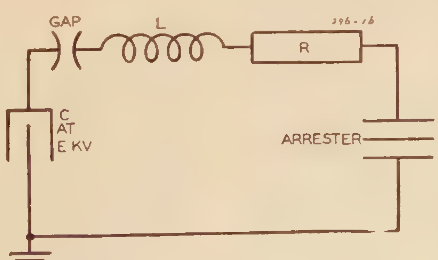

Figure 1b. Typical *RLC* impulse circuit

List of Symbols

The following symbols refer to the impulse-generator waves or circuit of Figure 1:

C = capacitance in microfarads
E = initial voltage on C in kilovolts
L = inductance in microhenrys
R = constant resistance in ohms
R_A = resistance of arrester*
R_T = total circuit resistance = $R+R_A$*
R_1 = critical resistance below which oscillations are produced
I_m = crest of current wave in kiloamperes
t_m = time in microseconds corresponding to I_m
t_2 = time in microseconds corresponding to $0.5I_m$ (on tail)
Δ = ratio of crest current for any half cycle to that of the preceding half cycle in an exponentially damped sine wave
E_m = crest of voltage wave produced by current wave through R_T

Typical Waves and Circuits

Figure 1a shows three current waves** and Figure 1b the customary circuit for producing such waves. The wave shapes I, II, and III are produced by circuits in which the total resistance is respectively

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*R_A and R_T are not constant but functions of voltage (or current) and time. Consequently R_A and R_T can only be represented completely as curves of instantaneous resistance plotted as a function of time. Such curves have a minimum at or a little after the maximum current.

**For time measurements in Figure 1a and throughout this paper, the actual zero is used. AIEE Standards No. 28 use the virtual zero which is the intersection on the time axis of a straight line projected through the 10 and 90 per cent points on the front of the wave. There is little practical difference, amounting to the order of only 1 per cent of the time to half value on the tail, for the waves here considered.

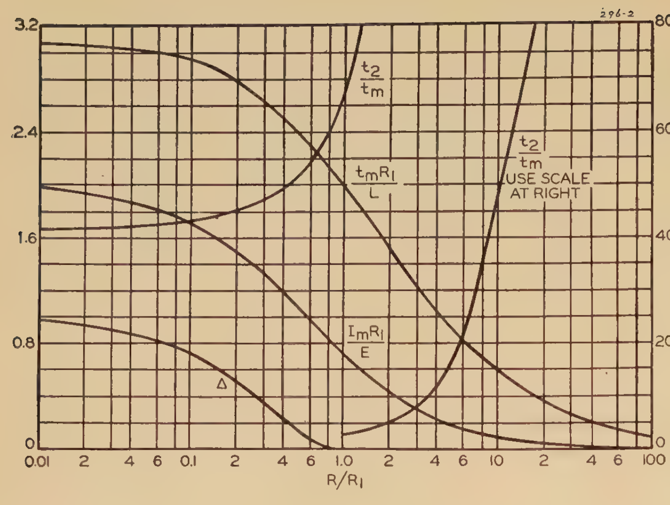

Figure 2. Effect of resistance on waves in *RLC* circuit

are so related that the following ratios are constant.

1. $t_m/\sqrt{LC}$
2. $t_2/\sqrt{LC}$
3. E/I_mR_T
4. E/E_m
5. I_mt_2/CE

It is possible to determine experimentally circuit constants to produce various wave shapes and by use of the above principles to calculate directly the constants required for other waves, similar in shape, but of different magnitudes or durations. Since the resistance may be a function of the current or voltage, the wave shape depends upon the voltage of the impulse generator as well as upon its other constants.

less than, equal to, or greater than the critical resistance. This total resistance R_T is composed of the constant resistance R and the nonlinear resistance R_A of the arrester. Valve-type arresters are designed to have a variable resistance R_A which is low at high currents but high at low currents, and R_A is the principal stumbling block to straightforward calculations of impulse circuits.

While both types of resistance may be present in any proportion, it will be shown how to compute the circuit constants when the resistance is predominantly linear or predominantly variable; from these, rough interpolations can be made for various combinations.

Before specific cases are considered, the principles of similarity and proportionality of circuit constants should be emphasized. Waves having similar shapes are those which can be represented by a single plot if either or both co-ordinate scales are multiplied by the necessary factors. In an *RLC* circuit as shown in Figure 1b the wave shape depends only upon $R_T/\sqrt{4L/C}$. If R_A is negligible, R_T is a constant and the above expression is a simple ratio of definite constants. However, if R_A is not negligible, R_T is really a function of time, as explained in the list of symbols. In this condition the expression is of little use; but with different waves of similar shape, the curves of R_T versus time also are similar, and any two corresponding values of R_T, such as those at maximum current, can be used for comparison purposes.

For current waves of similar shape, the circuit constants and wave specifications

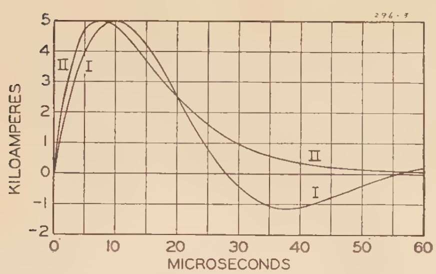

Figure 3. Impulse-current waves calculated by curves of Figure 2

		Wave I	Wave II
Given	C	1	1
	t_m	10	—
	t_2	20	20
	I_m	5	5
	Shape	—	Critically damped
Found From Curves	E	68.5	101
	L	64.0	55.5
	R	6.88	14.9

Wave I. $I=9.5\epsilon^{-0.054t}\sin 0.113t$
Wave II. $I=1.18t\epsilon^{-0.134t}$

Calculations When Arrester Resistance Is Low in Comparison With Total Circuit Resistance

For the cases where the arrester resistance can be neglected (or lumped with the circuit resistance) the current for any particular circuit can be found easily from the well-known equations:

$$I=(E/\omega L)\epsilon^{-at}\sin\omega t \quad \text{where } R<R_1 \tag{1}$$

$$=\frac{Et\epsilon^{-at}}{L} \quad \text{where } R=R_1 \tag{2}$$

$$=\frac{E}{L}\,\frac{\epsilon^{-mt}-\epsilon^{-nt}}{n-m} \quad \text{where } R>R_1 \tag{3}$$

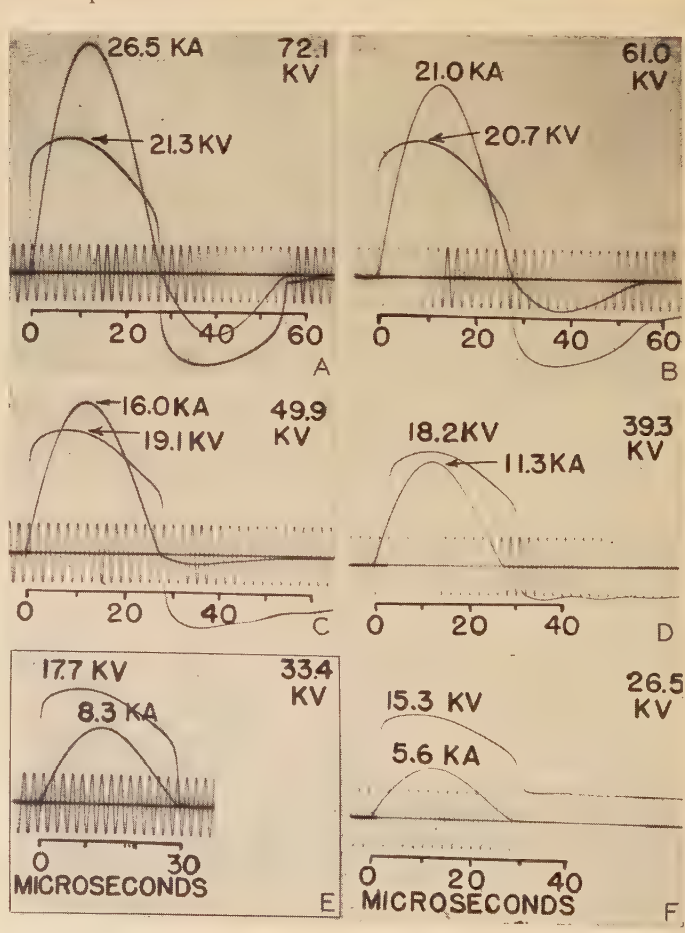

Figure 4. Current and voltage oscillograms on lightning-arrester element in *RLC* circuit of Figure 1b

$C=4.41$; $L=15.4$; $R=0.142$. The same arrester element was used for all six waves with the wave shape determined only by E which is listed in upper right-hand corner of each oscillogram

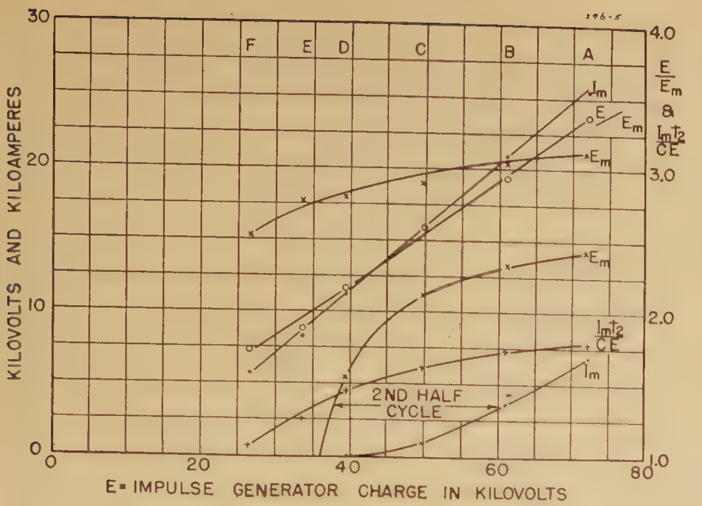

Figure 5. Curves of E_m and I_m taken from oscillograms of Figure 4

Curves of E/E_m and $I_m t_2/CE$ are computed from these results, but are applicable for any other combinations of circuit constants which can produce similar wave shapes

$$a=\frac{R}{2L};\ \omega=\sqrt{\frac{1}{LC}-a^2};\ m=\frac{R-\sqrt{R^2-R_1^2}}{2L};$$

$$n=\frac{R+\sqrt{R^2-R_1^2}}{2L}$$

Equation 1 defines an oscillatory wave such as wave I in Figure 1a, while equations 2 and 3 are for the nonoscillatory waves II and III, respectively.

Equations 1, 2, and 3 are transcendental and it is not possible to calculate directly the equation for a specified impulse such as a 5,000-ampere 10x20-microsecond wave. It is best to assume various circuit constants and plot curves which then can be used directly. It is found that for the *RLC* circuits the shape of the current wave depends only upon the ratio R/R_1 and this ratio is the most convenient parameter for abscissa of the curves. Figure 2 shows a set of such curves with ratios of R/R_1 from 0.01 to 100. The left-hand side of Figure 2 represents oscillatory conditions and the right-hand side nonoscillatory conditions. For a 5x10- or 10x20-microsecond wave, $t_2/t_m=2$, and it is found that such a relation holds only for oscillatory waves for which $R/R_1=0.43$ and $\Delta=0.22$. For the critically damped waves $R/R_1=1$ and $t_2/t_m=2.68$.

An infinite number of circuits and voltages will produce any possible wave, and at the start one constant must be arbitrarily fixed. Usually the first selected constant is either the impulse-generator capacitance or voltage, but in some circumstances the limiting value might be inductance or resistance. Figure 3 shows two waves for which the impulse-generator capacitance was fixed and the remaining constants determined by use of the curves in Figure 2. It is seen that the two waves are not greatly different, but considerably more charge was required for the critically damped wave. For any nonoscillatory wave, the area under the ampere–time wave in ampere-seconds is equal to the charge CE (C in farads and E in volts) but for oscillatory waves an integration for the first half cycle shows the ampere-seconds to be $CE(1+\Delta)$, and providing the resistance can be kept low enough, it is possible to get in the first half cycle nearly twice the ampere-seconds originally stored in the capacitance. This means, however, that the wave will oscillate for many half cycles, which frequently is not a desirable condition.

Empirical Calculations to Include Nonlinear Resistance of Arrester

Valve-type lightning arresters are designed to have low resistance at high currents and high resistance at low currents, and rigorous calculations of circuits containing such resistances are not possible. Figure 4 shows the types of waves which can be obtained in the circuit shown in Figure 1b where the variations in waves are caused only by variations in the charging voltage. Considering only the first half cycle, the current waves are remarkably similar in shape and are all approximately 12.5x22-microsecond waves. This ratio of t_2/t_m of about 1.75 is typical of impulse-generator circuits in which the arrester-valve element comprises most of the resistance, and so the specified ratio of two for 5x10- or 10x20-microsecond waves can be fulfilled approximately without difficulty. If a closer agreement is desired, it can be obtained by using some additional constant resistance.

In addition it should be observed that the first quarter cycle is practically a sine wave and the time to crest of 12.5 microseconds is a close approximation of the calculated time to crest for no resistance where:

$$t_m=0.5\pi\sqrt{LC} \tag{4}$$

which gives $0.5\pi\sqrt{4.41\times15.4}=13$ microseconds. In Figure 5 are plotted the crest measurements of current and voltage from Figure 4, and in addition the ratios E/E_m and $I_m t_2/CE$.

The curves for I_m and E_m apply only for the circuit constants listed for Figure 4, but the ratios E/E_m and $I_m t_2/CE$ are universal in application for circuits wherein the resistance is chiefly arrester valve element. The absolute magnitudes and durations are immaterial for these ratios which depend only upon the wave shapes. The points for the ratios are identified with letters A–F corresponding to the oscillograms A–F in Figure 4. Hence, in any circuit if the wave shape of Figure 4E is desired, E/E_m must be 1.97. and $I_m t_2/CE$ must be 1.31. In the calculation of such a circuit, it is necessary to know or assume the maximum voltage across the arrester and resistance expected at the desired current. The following example shows the general method of attack.

Example

A 75-kv impulse generator is available with a maximum capacitance of seven microfarads and a minimum inductance of two microhenrys. Determine if this equipment is capable of producing a 100,000-ampere 5x10-microsecond discharge through an arrester having approximately 18-kv crest on this wave. Here $I_m t_2/CE=1.9$.

The curve of $I_m t_2/CE$ in Figure 5 goes only to 1.8 so the desired wave, if at all

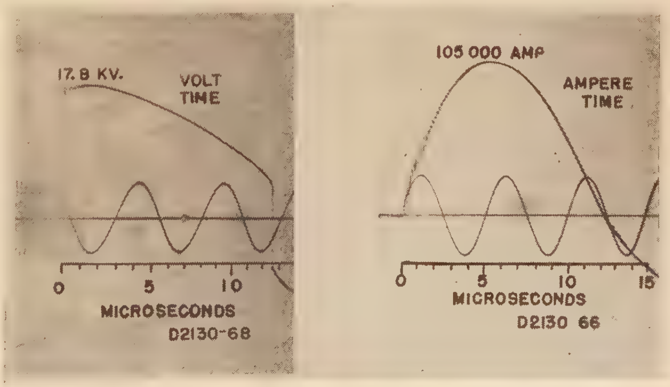

Figure 6. Voltage and current oscillograms for nominal 100,000 - ampere 5 x 10-microsecond current wave through a valve-type lightning arrester

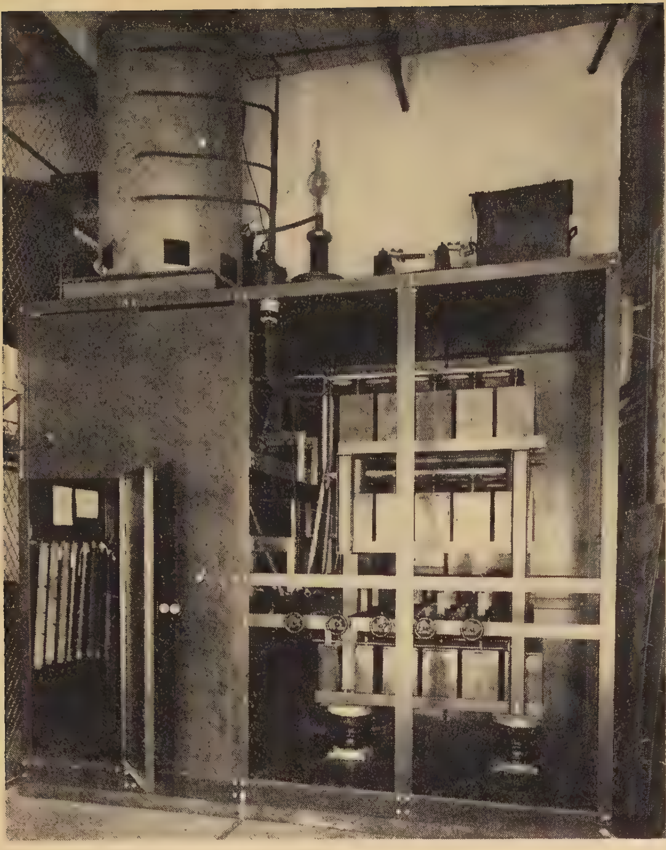

Figure 7. Adjustable-capacitance impulse generator number 1

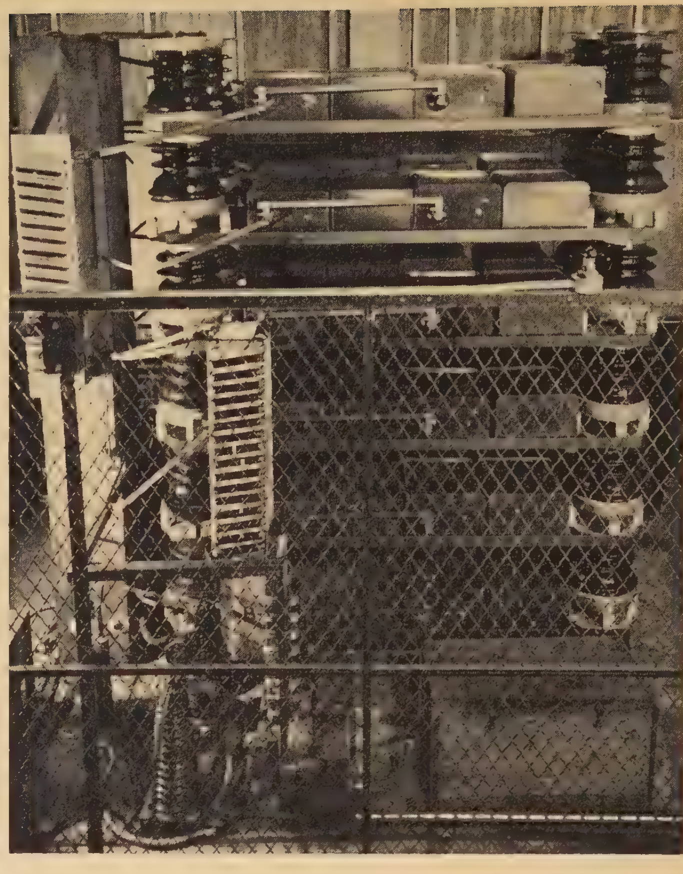

Figure 9. Marx-circuit impulse generator number 2

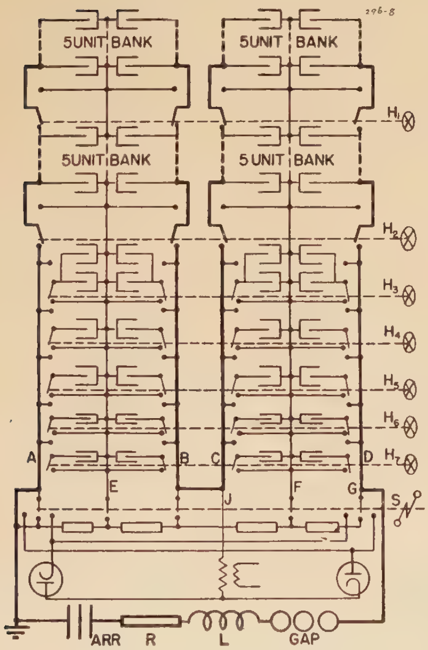

Figure 8. Circuit diagram of impulse generator number 1

Hand-operated switches H_1–H_7 throw capacitor units or banks on the busses *ABCD* or disconnect and short-circuit the terminals to the cases, *EF*. Solenoid-operated switch *S* has three positions: "left" for positive generator charge; "right" for negative generator charge; "middle" automatically grounds all live capacitor points except during charging periods

possible, will be more oscillatory than that shown in Figure 4A. Since some tolerance is allowed, a 4.7x9.4-microsecond wave is acceptable, which gives $I_m t_2/CE = 1.8$. Hence there is sufficient charge available to produce a 4.7x9.4-microsecond wave similar in shape to Figure 4A.

Using equation 4, $t_m = 0.5\pi\sqrt{LC} = 5.9$ microseconds, which is close enough for practical purposes.

For $I_m t_2/CE = 1.8$, the corresponding value of E/E_m is 3.38. This means that sufficient additional resistance should be added to the circuit so that $E_m = 75/3.38 = 22$ kv.

Figure 6 shows waves actually produced under the conditions specified in the above example. However, no resistance was added, so that for the waves of Figure 6 the current is higher and the oscillations greater than in the example.

The foregoing example shows that it is not always possible to fulfill the exact requirements determined by calculation, and in order to test various types and ratings of arresters over a range of currents and wave shapes, the impulse-generator constants and charging voltage must be adjustable over a wide range.

When several ratings of arresters are to be tested with the same wave, the circuit adjustments are minimized by setting up to test the highest rating first, and when other ratings of lower internal resistance are substituted, sufficient resistance is added to keep the total resistance constant.

Often it is found impossible, with existing equipment, to get aperiodic waves coming rapidly to zero as in Figure 4E. If the maximum charging voltage E is too low for a particular arrester rating, the waves will look like Figure 4F. If there is current to spare, the voltage wave can be brought down to zero by the use of a constant resistor in parallel with the arrester or generator capacitance. If the product CE is too small to produce the required wave like Figure 4E, the current can be increased in low ratings of arresters by letting it oscillate somewhat as was done in Figure 6 or Figure 4 (A–D).

Calculation for a Wave of Specified Crest and Nominal Steepness

For a wave specified by steepness instead of duration, it is only necessary to redesignate it in the usual way and calculate the circuit constants as previously shown. For circuits where R is low compared to R_T, the first quarter cycle is almost a sine wave. The nominal steepness, or the slope of the line through the

10 per cent and 90 per cent points, is approximately

$$\text{Nominal steepness} = 1.2I_m/t_m \qquad (5)$$

or with $t_2/t_m = 1.75$ as in the waves of Figure 4

$$\text{Nominal steepness} = 2.1I_m/t_2 \qquad (6)$$

For critically damped waves in circuits where R_A is low compared to R_T, similar approximate equations are

$$\text{Nominal steepness} = 1.4I_m/t_m \qquad (7)$$

or

$$\text{Nominal steepness} = 3.7I_m/t_2 \qquad (8)$$

for intermediate wave shapes an approximate equation is

$$\text{Nominal steepness} = 1.3I_m/t_m \qquad (9)$$

and the equation in terms of t_2 can be found using the ratios of t_2/t_m in Figure 2.

Hence, using the equations 5 to 9 any wave based on crest and nominal steepness can be redesignated by crest and duration and calculations made as before.

Control of Impulse-Current Wave Shapes in General Testing

The calculating methods here presented are also applicable where nonlinear resistances, such as Thyrite, are used to control impulse-current wave shapes.[12,13] The use of such resistors permits higher crest currents without oscillation, and shorter wave tails, than can be obtained by use of constant resistance.

Description of Impulse Generators

It is evident from the foregoing material that lightning arrester impulse testing requires an extended range of circuit constants and unless the various circuits can be conveniently and quickly set up, much valuable time will be lost. In addition to the very high voltage and very high current impulse generators necessary for special tests on lightning arresters, there have been built during the last few years four generators at the continuous disposal of the lightning arrester department in Pittsfield.

Generator Number 1 for General Testing

Generator number 1 is shown in Figure 7 with part of the steel panels removed. The capacitors are all Pyranol-filled 75-kv units especially designed to have low inductance and to withstand repeated short-circuit currents. All units have mid-points tied to the cases. There are 28 capacitors rated 0.24 microfarad and 4 trimmers for fine adjustments rated 0.06 microfarad. The charge and discharge circuits are connected to two pairs of copper-tubing busses which may be operated in series for 150 kv or in parallel for 75 kv. The capacitors are mounted on two three-deck steel frameworks insulated from ground and each other. At 150 kv connection, the cases of capacitors in the front framework are at 112.5 kv and those in the back framework are at 37.5 kv. The handwheels shown in Figure 7 throw two or more capacitors on the lower busses or short circuit them to their respective frameworks. The capacitors in the two upper decks are permanently bussed in parallel but they can be connected either to the lower busses or short-circuited to the framework. The charging equipment is located on top of the steel housing, and the large cylinder contains a combination polarity reversing switch and ground switch which grounds all busses and frameworks except during the charging period.

Figure 8 shows the elementary circuit diagram for the 150-kv connection. For clarity none of the hand-operated switches H_1–H_7 are shown completely closed, but H_1 and H_2, when thrown to the indicated position, connect the two upper decks to the busses $ABCD$ while the capacitors in the lower deck are all removed from the busses and short-circuited by the switches H_3–H_7. By means of the switches any capacitance from 0.04 to 1.75 microfarads in steps of 0.04 microfarad can be instantly selected for 150-kv operation. For 75-kv operation switches (not shown in Figure 8) connect bus D to bus A and mid-point E to mid-point F. Also G is moved from D to BC and J from BC to EF. With these connections the capacitance can be varied from 0.16 to 7 microfarads in steps of 0.16 microfarad. For very long waves, all the capacitor bushings can be bussed together, using the cases for the return circuit, and then 28 microfarads can be obtained for charging voltages up to 37.5 kv.

Tests at moderate currents are made in the oscillograph room at the rear of the generator. High-current tests require short leads and are made inside the steel housing. Some of the tapped resistance and inductance control elements are shown hanging on the wall inside the door. The minimum discharge inductance is about two microhenrys and tests up to 100 kiloamperes can be made on low-voltage arresters. The oscillograms of Figure 6 were obtained using this generator.

The advantages of this impulse generator are:

1. *Safety.* No power can be applied until all safety gates are closed and all capacitance parts are grounded after every discharge.

2. *Convenience.* Capacitance and voltage are variable over wide ranges with little effort. The polarity can be changed by the flip of a switch. The three-electrode gap spacing is controlled by a wooden shaft extending into the oscillograph room, and the charging-voltage control is located on the oscillograph table.

3. *Reliability.* Several years of service without trouble have demonstrated its reliability.

4. *Compactness.* A minimum of floor space is used, the dimensions being $6^1/_2$ by 12 feet.

5. *Totally Enclosed.* The steel housing eliminates stray electrostatic fields, and the exterior is neat even when destructive tests are being made inside. It also reduces the noise when high-current tests are being made.

Generator Number 2 for General and High-Voltage Testing

Impulse generator number 2 has a 600-kv six-stage Marx circuit as shown in Figure 9. The Pyranol-filled capacitors are rated 2.2 microfarads at 25 kv and have but one bushing with the case serving as the other terminal. Each stage consists of two groups of four capacitors in series. The primary groups are shown on the left in Figure 9 and the secondary groups at the right can be connected in parallel or short-circuited and left with cases floating. The charging resistors and interstage sphere gaps are mounted on the vertical wooden box at the extreme left. By means of switches the generator can be connected conveniently to operate at reduced voltage and high capacitance, and, if desired, part of the capacitance can be omitted. The following combinations can be obtained with either polarity and any voltage up to the maximum listed:

Maximum Kv	Microfarads Available
50	1.1 to 26 in steps of 1.1
100	0.55 to 6.60 in steps of 0.55
200	0.28...0.55...0.82...1.1...1.6
300	0.18...0.37...0.73
400	0.14...0.28
500	0.11...0.22
600	0.09...0.18

The advantages of this generator over number 1 are its higher voltage and greater energy. Tests can be made on arresters up to 75-kv rating at 5,000 amperes, 10x20-microsecond waves, and at appreciably greater currents on lower ratings. Its minimum inductance is higher than for generator number 1 since the Marx circuit necessitates longer connections. Because the minimum inductance at 100 kv is about 3.5 microhenrys, generator number 2 will not produce as high currents as generator number 1 even though it contains nearly twice the energy.

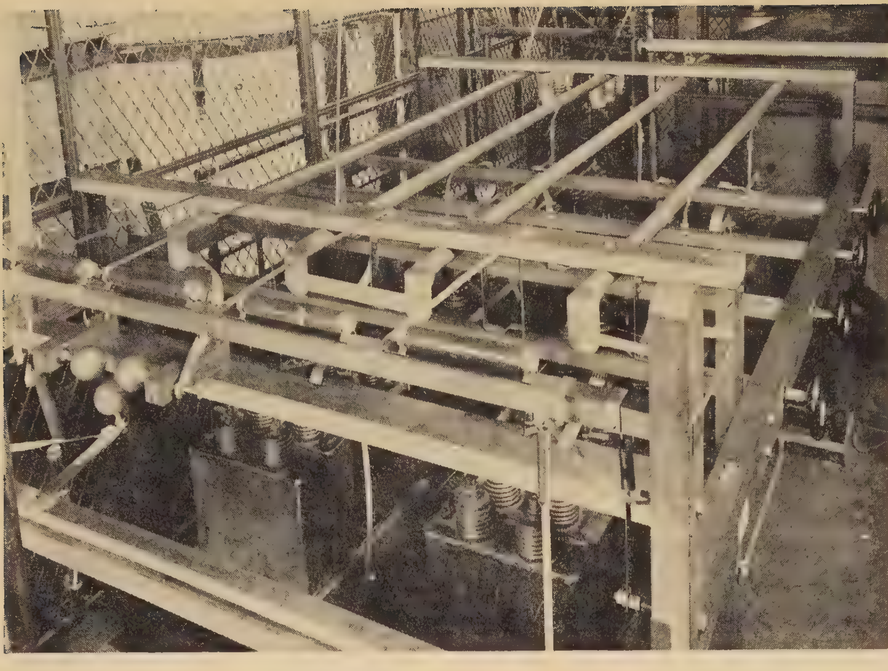

Figure 10. Adjustable-capacitance impulse generator number 3 used for operating-duty power tests

However, for a wide range of voltage and moderate currents of 50 kiloamperes or less, it supplements the other generator. The oscillograms in Figure 4 and all those in a contemporary paper[14] describing a new oscillograph were produced using this generator.

Generators Numbers 3 and 4 Used for Operating-Duty Tests

For convenience, two additional impulse generators are used for operating-duty power tests. Generator number 3, shown in Figure 10, consists of two rows of capacitors which can be connected in parallel or series for 50- or 100-kv operation. The usual connection is 100 kv, and by means of the handwheel-operated switches, the capacitance can be adjusted from 0.125 to 1.9 microfarads in steps of 0.125 microfarad. Units not in use are short-circuited by the switches, but the cases remain at the midtap potential of those in use. All live parts are automatically grounded except when the generator is on charge. The voltage and gap settings are easily adjustable, and the polarity can be reversed if desired.

Generator number 4 is similar in construction and rating to number 3. Either one is capable of sending a 10,000-ampere 10x20-microsecond wave through a 12.5-kv arrester. Number 3 is used for routine tests with a power supply of 500 kva at 3, 6, 12, or 24 kv at 60 cycles, while number 4 is used with a supply of 2,500 kva at 7.5 or 15 kv.

In addition to the four impulse generators described, there are available in the high-voltage engineering laboratory impulse generators rated up to 5,000 kv and a high-current generator[11] which can be connected for a maximum 6.2 microfarads at 150 kv, 14 microfarads at 100 kv, or 56 microfarads at 50 kv. This high-current generator has very low inductance and can produce currents up to 250 kiloamperes. It can be used for regular impulse tests or for operating-duty power tests with either of the power sources used with generators number 3 or 4.

Summary

1. Thorough testing of lightning arresters requires a wide variety of impulse-current waves.

2. The strictly mathematical calculation of circuit constants to produce such waves is limited to the few cases where the circuit resistance is constant.

3. Valve-type lightning arresters have a resistance that is intentionally a function of voltage or current and the determination of the constants of circuits containing valve elements must be done experimentally.

4. Impulse-current waves of the same shape but various durations and magnitudes have circuit constants where $R_T/\sqrt{4L/C}$ is fixed.

5. Once the circuit constants are determined for a particular wave, the constants for similar waves of other magnitudes and durations are easily calculated.

6. The curves of Figure 5 facilitate such calculations for the wide range of wave shapes shown in Figure 4.

7. The testing of various types and ratings of arresters with a number of different waves, requires extremely flexible impulse-generator circuits.

8. Ordinarily the impulse-generator capacitance is the most difficult circuit constant to adjust, but four special impulse generators are described having capacitance values easily adjustable over a wide range. Several years of experience with these generators, and the previously mentioned calculating methods, show that constants for any desired wave, within the capabilities of the equipment, can be computed and set up easily and quickly.

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Analytical Treatment for Establishing Load-Cycle Ratings of Ignitrons

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THE ignitron offers unique possibilities as a control element for high instantaneous alternating demand currents. The demand current is limited only by the capacity of the ionized vapor to conduct the current from the mercury pool to the anode and by probability of arc back, which increases as the peak current conducted by the tube increases. The design of ignitron tubes is a compromise between these two factors. Design for low arc-back rate usually results in anode baffles and shields which tend to increase the deionizing properties of the tube and thus decrease the peak current-carrying capacity.

The above consideration and not failure of the cathode to emit electrons limits the peak current that can be controlled by an ignitron. For various classes of control service, such as resistance welder control, the arc-back problem is minimized by the characteristics of the circuit, and the tubes are designed without baffles or other constrictions. Therefore, the peak to average current ratios can assume higher values than are customary in gas- or vapor-filled tubes in other classes of service.

Experience has indicated that the average current ratings within the above peak current limitation are limited mainly by thermal considerations. These limitations are relatively easy to determine under conditions of continuous operation. One operates the tube for a reasonable period of time with the circuit constants and the firing phase chosen so as to place the maximum arc-back stress on the tube at regularly increasing current steps until arc back or loss of control occurs and then backs off for a reasonable factor of safety to establish a point on the rating curve at 100 per cent duty.

By measuring the loss in the tube at this current the energy which can safely be dissipated by the tube can be determined for continuous duty.

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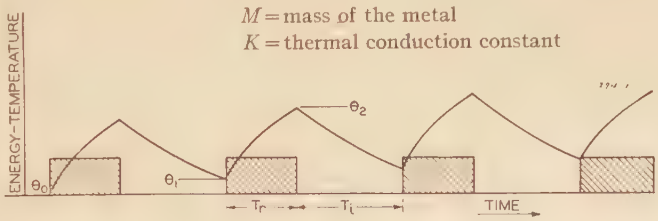
However, tubes in general and welding tubes in particular are seldom operated at continuous duty. A large number of combinations of duty cycle and demand currents is possible for a given size ignitron and is used in commercial practice. It is, therefore, necessary to invent methods of determining rationally the rating of these tubes and presenting the data in a form which is capable of furnishing a definite figure for the permissible demand current at any conceivable combination of current and duty.

Obviously, the tube cannot be economically tested for all the various possible combinations. The present paper describes a method which can be used to interpolate or extrapolate to obtain ratings for any combination of intermittent duty. This method consists in operating the tube at a simple on- and off-duty cycle at a high current value and determining an intermittent rating point in addition to the continuous rating point determined as described above.

Curves of arc drop as a function of demand current are also determined. It will in general be found that the average loss in the tube for equal excellence of operation at high currents will be less than that at continuous duty. It is assumed that this difference in heat-dissipation quality is due to the intermittent production of heat in the tube and is a function of an equivalent "thermal time constant" similar to the equivalent electrical quantity. The accuracy of the method can be checked in practice by repeating the determination of the thermal constants using several different duty cycles. The authors realize that the variation of the temperature of the simple model treated in the following discussion does not completely describe the thermal cycle of an ignitron but believe that by its use a close enough approximation can be achieved for the degree of accuracy required.

Figure 1. Temperature of model as function of time, with rectangular-waveform energy input



Similar methods are now in use employing the familiar concept of maximum averaging time and are satisfactory enough for most purposes but give results that seem artificial under certain conditions. It is hoped that the present paper will be regarded as a theoretical approach to a rating method based on more nearly correct physical principles.

Thermal Action of Equivalent Model

In approaching the problems of establishing ratings for ignitrons on intermittent duty, equations are developed for a model system. This system is one in which heat is lost by conduction, and the heat loss is therefore proportional to the first power of the temperature difference between the model and the cooling medium. The model is considered to be a small mass of metal in which the temperature at any time is uniform throughout. The mass of metal is surrounded by a cooling medium to which heat from the metal is lost by conduction.

The case to be considered is one where energy is put into the system in equal amounts at regularly spaced intervals. During the time that energy is being put into the system the temperature rises, although some heat is lost by conduction. During the off period when no energy is being supplied, the temperature falls.

To develop the general equation for intermittent loading it is necessary to set up relations between the temperature θ' and the energy input W and time t for both the heating and cooling portions of the cycle. The differential equation for the temperature, while energy is being supplied at the rate W, is

$$CMd\theta' = Wdt - K(\theta' - \theta_0)dt$$

If temperature rise $\theta' - \theta_0$ is replaced by θ, and consequently $d\theta'$ by $d\theta$, the equation will express the temperature θ referred to the temperature of the cooling medium as zero, giving

$$CMd\theta = Wdt - K\theta dt \tag{1}$$

where

θ = the temperature of the heated mass referred to the cooling medium temperature as zero
W = average watts input during the heating period
C = specific heat of the metal
M = mass of the metal
K = thermal conduction constant

Rearranging the terms in equation 1 and integrating:

$$\log_\epsilon (W-K\theta)=\text{ constant}-\frac{K}{CM}t$$

and since at $t=0$, $\theta=\theta_1$, the constant $=\log_\epsilon (W-K\theta_1)$, where θ_1 is the temperature at the beginning of the heating period. Solving for θ, the temperature at any time during the heating period is given by

$$\theta=\left(\theta_1-\frac{W}{K}\right)\epsilon^{-\frac{t}{T_c}}+\frac{W}{K}\quad\text{(heating period)}\qquad(2)$$

where

$$T_c=\frac{MC}{K}=\text{time constant}$$

The time factor $k=T_r/(T_r+T_i)$ is the ratio of the time during which the load is on, to the period or length of the cycle. On the initial cycle θ_1 is equal to θ_0, and θ_2 is given by equation 2 where t is set equal to T_r. For each successive heating period a different value of θ_1 must be used, and this is of course the value of θ at the end of the previous cooling period. After a sufficient number of impulses equilibrium will be reached, and then all the successive values of θ_1 will be equal and the values of θ_2 will also be equal. This simply means that in each "off" period the system loses just the amount of heat which it gained in the previous "on" period.

The allowed value of the watts W during the on period is determined by the

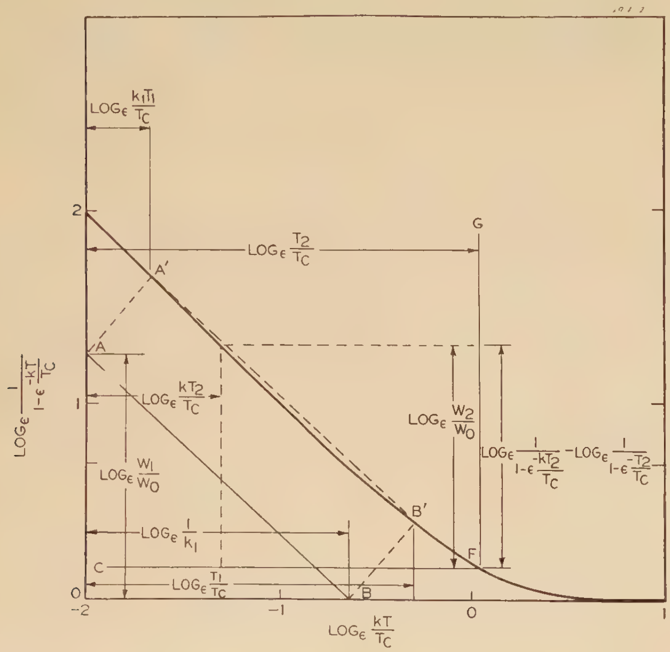


Figure 2 (left). Chart illustrating graphical method of determining rating of model as function of duty cycle and thermal time constant

Substituting this value of θ_1 in the second term of equation 5

$$\theta_2=\frac{W}{K}\left(\frac{1-\epsilon^{\frac{T_r}{T_c}}}{1-\epsilon^{\frac{T_r+T_i}{T_c}}}\right)\epsilon^{\frac{T_i}{T_c}}$$

and simplifying

$$\theta_2=\frac{W}{K}\left(\frac{1-\epsilon^{-\frac{T_r}{T_c}}}{1-\epsilon^{-\frac{T_r+T_i}{T_c}}}\right)\qquad(7)$$

We may now designate the period of the intermittent cycle, (T_r+T_i), by the symbol T as is customary. Then since the ratio of conducting time to the period $T_r/(T_r+T_i)=T_r/T=k$, equation 7 becomes

$$\theta_2=\frac{W}{K}\left(\frac{1-\epsilon^{-\frac{kT}{T_c}}}{1-\epsilon^{-\frac{T}{T_c}}}\right)\qquad(8)$$

Equation 8 gives the value of the maximum temperature reached at equilibrium in terms of the watts input W, the period T, and the "on" time $kT=T_r$. The safe or allowed value of θ_2 can be obtained from the allowed watts, W_0, for continu-

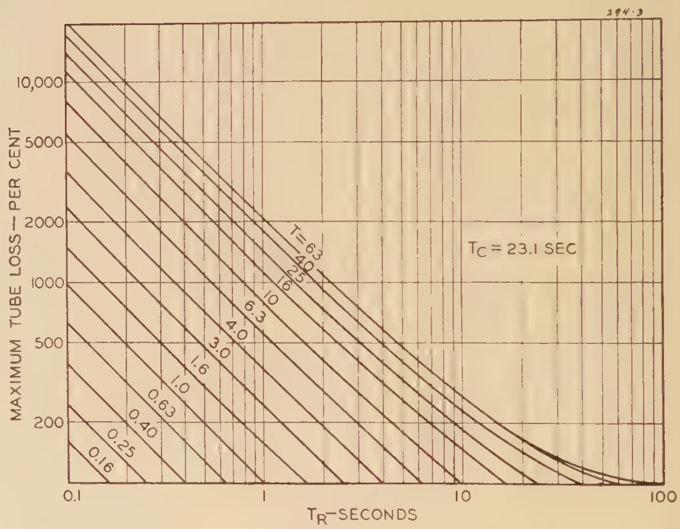


Figure 3 (right). Curve showing rating of model at a given thermal time constant equal to 23.1 seconds

The temperature during the cooling period can be found from equation 1 by letting $W=0$. This gives

$$\log_\epsilon \theta=\text{constant}-\frac{t}{T_c}$$

At $t=0$ that is, at the end of a heating period $\theta=\theta_2$ as given by equation 2 when t equals T_r, the length of the heating period. This makes the constant equal to $\log_\epsilon \theta_2$ and gives

$$\theta=\theta_2\epsilon^{-\frac{t}{T_c}}\quad\text{(cooling period)}\qquad(3)$$

Equations 2 and 3 are sufficient to determine the temperature θ at any time during load-cycle operation.

Regular Intermittent Duty

Figure 1 shows the temperature θ for a series of regularly spaced equal impulses supplied as shown by the shaded blocks.

maximum temperature allowed. Therefore the value of θ_2 must be found after equilibrium has been reached. For this condition $\theta=\theta_1$ at $t=T_i$ in equation 3 and rearranging

$$\theta_2=\theta_1\epsilon^{\frac{T_i}{T_c}}\qquad(4)$$

Also, θ_2 in equation 4 must equal θ from equation 2 when $t=T_r$ and therefore

$$\theta_2=\theta_1\epsilon^{\frac{T_i}{T_c}}=\left(\theta_1-\frac{W}{K}\right)\epsilon^{-\frac{T_r}{T_c}}+\frac{W}{K}\qquad(5)$$

Solving for θ_1

$$\theta_1=\frac{W}{K}\left(\frac{1-\epsilon^{\frac{T_r}{T_c}}}{1-\epsilon^{\frac{T_r+T_i}{T_c}}}\right)\qquad(6)$$

ous loading. In this case $k=1$ and equation 8 gives

$$\theta_{2\ \max}=\frac{W_0}{K}\qquad(9)$$

If the requirement for safe operation is that θ_2 shall never exceed W_0/K, it can be replaced by this value in equation 8, giving

$$\frac{W}{W_0}=\left(\frac{1-\epsilon^{-\frac{T}{T_c}}}{1-\epsilon^{-\frac{kT}{T_c}}}\right)\qquad(10)$$

This equation gives the allowed value of input watts W, which can be used for a time, $T_r=kT$ for a repeated loading cycle of period T.

In order to use a graphical construc-

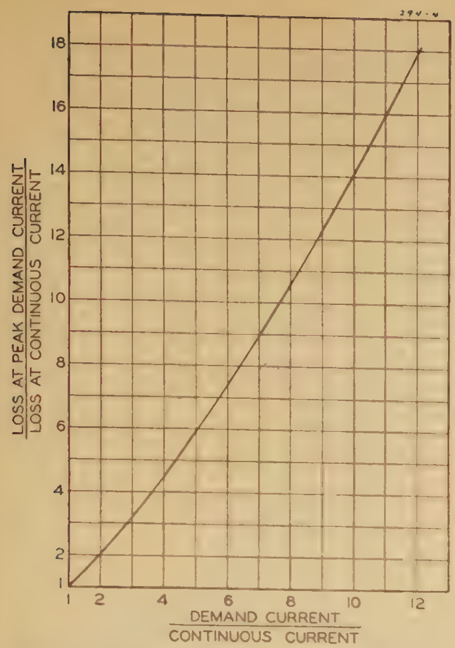


Figure 4. Curve showing increase in tube loss as function of increase in tube-demand current

tion from which the value of time constant can be determined, and rating curves can be laid out, it is convenient to consider equation 10 in the following manner. For two values of k, but the same period T

$$\frac{W_1}{W_0}=\frac{1-\epsilon^{-\frac{T}{T_c}}}{1-\epsilon^{-\frac{k_1T}{T_c}}} \tag{11}$$

and

$$\frac{W_2}{W_0}=\frac{1-\epsilon^{-\frac{T}{T_c}}}{1-\epsilon^{-\frac{k_2T}{T_c}}} \tag{12}$$

Dividing equation 11 by equation 12

$$\frac{W_1}{W_2}=\frac{1-\epsilon^{-\frac{k_2T}{T_c}}}{1-\epsilon^{-\frac{k_1T}{T_c}}} \tag{13}$$

This expression suggests the relations

$$W_1\propto\frac{1}{1-\epsilon^{-\frac{k_1T}{T_c}}} \tag{14}$$

and

$$W_2\propto\frac{1}{1-\epsilon^{-\frac{k_2T}{T_c}}} \tag{15}$$

Figure 2 shows $\frac{1}{1-\epsilon^{-\frac{kT}{T_c}}}$ plotted against kT/T_c. This curve will give the value of W/W_0 by dividing the value for k_1T/T_c by that for T/T_c. If the value of T_c is known, specific values of T_r and T can also be derived from this curve.

Application to Ignitron Ratings

As an illustration of the use of this method, rating curves will be obtained for a small water-cooled metal ignitron. Since all of the energy loss is eventually transferred to the cooling water, the problem will be regarded as one where heat is lost by conduction, as in the system covered by the preceding sections.

The test data were as follows:

1. Continuous duty
Tube loss at satisfactory operation $=W_0$

2. Intermittent duty
Tube loss at equally satisfactory operation $=W_1$
$T_r=$ 0.5 second
$T=11.1$ seconds
$k=$ 0.0448

3. $\frac{W_1}{W_0}=18.0$

To find T_c substitute in equation 10

$$\frac{W_1}{W_0}=18.0=\frac{1-\epsilon^{-\frac{11.1}{T_c}}}{1-\epsilon^{-0.0448\left(\frac{11.1}{T_c}\right)}}$$

This equation can be solved by trial for $11.1/T_c$. The solution is 0.48 ± 0.005

$$\text{or } T_c=\frac{11.1}{0.48\pm0.005}=23.1\pm0.3 \text{ seconds}$$

Since ignitrons are rectifiers the current conducted by them on a-c circuits is not continuous but consists of a series of half sine waves. The foregoing theory assumes that this intermittent power input can be represented by a continuous function of the time. The accuracy of this assumption can be checked by use of equation 4 where

$$\frac{\theta_2}{\theta_1}=\epsilon^{\frac{T_i}{T_c}}$$

Using the data of the above example and a line frequency of 60 cycles per second

$$\frac{T_i}{T_c}=\frac{\frac{1}{120}}{23.1}=0.00036$$

and therefore

$$\frac{\theta_2}{\theta_1}=1.0004$$

This result indicates that in this case the variation in temperature due to the rectified current pulsation in the tube can be neglected in comparison with the longer period load cycling.

Graphical Calculation and Presentation of Data

Assume that test data are available to establish a rating at a given T and T_r, and that the ratio of the loss in the tube at continuous duty to that at the given duty is known, then the above method will permit the calculation of a value for the thermal time constant, T_c.

The thermal time constant will allow a system of interpolation of the results of the rating tests to other load cycles in a systematic way which is mathematically continuous. The present method, wherein a "maximum averaging time" is used, has the effect of limiting the maximum time of impulse to values less than the maximum averaging time even for demand current loads of only a few per cent higher than continuous load rating. This new method goes over smoothly to the continuous rating.

Graphical Calculation of T_c

The function $\frac{1}{1-\epsilon^{-\frac{kT}{T_c}}}$ is plotted in Figure 2 on logarithmic co-ordinate scales. Our test data give us two values of tube loss (W_1 and W_0) at two values of T_r equal to kT_1 and T_1 respectively, where T_1 is the impulse period of the rating test. It is desired to determine the value of T_c which fixes the scale of the diagram, Figure 2, for the particular tube being rated.

From equation 11 we can write

$$\log_\epsilon\frac{W_1}{W_0}=\log_\epsilon\frac{1}{1-\epsilon^{-\frac{k_1T_1}{T_c}}}-\log_\epsilon\frac{1}{1-\epsilon^{-\frac{T_1}{T_c}}}$$

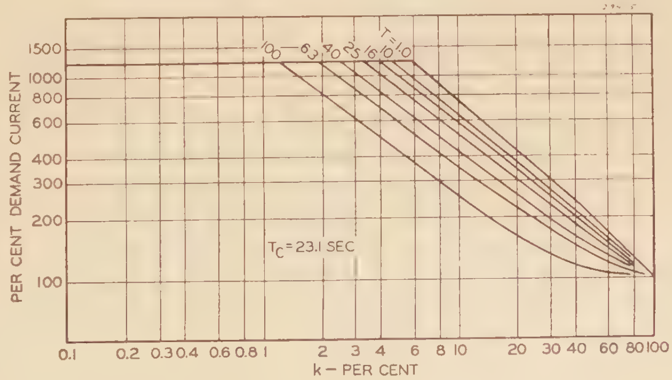


Figure 5. Example family of tube-rating curves as function of impulse time, T_r, and impulse period, T, for thermal time constant, T_c=23.1 seconds. Calculated from Figures 3 and 4

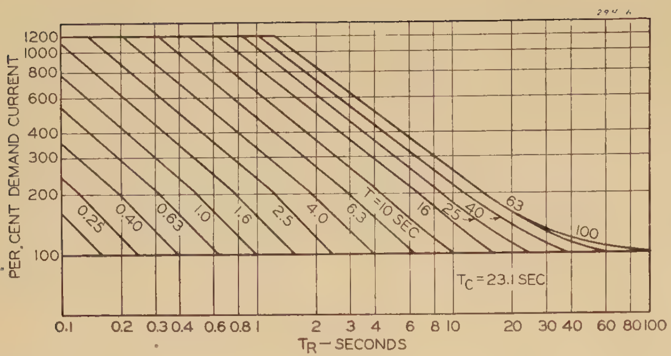


Figure 6. Example family of tube-rating curves as function of per cent duty and impulse period. Calculated from Figure 4

These curves are sharply cut off at the peak current value of 1,200 per cent.

The same information is given in curve Figure 6 in another form. Here, each of the abscissa is in values of k as per cent of the value of T used as parameter on the individual rating curves. This may be compared more directly with the present rating curves where the abscissa is plotted in per cent duty. T here compares with the present "maximum averaging time."

It is seen that the relationship existing between W_1/W_0 and k_1 for given values of T and T_c is in general given by the first term of the above equation, since when T and T_c are fixed, the second term is independent of k_1 and becomes of a constant value which can be subtracted graphically as is done in Figure 2. In order to determine T_c, it is necessary to determine the amount to be subtracted from the ordinate of the curve in Figure 2, in order to make the remainder of the curve fit the conditions as laid down by the test.

This can be done most easily in the following manner. Since the argument of the plotted function is kT/T_c and since the time difference of the test points is defined by k_1, the difference in the abscissa of the test points on the curve at A' and B', when found, must be equal to

$$\log_\epsilon \frac{T_1}{T_c} - \log_\epsilon \frac{k_1 T_1}{T_c} = -\log_\epsilon k_1 = \log_\epsilon \frac{1}{k_1}$$

Therefore, the slope of the chord at $A'B'$ will be

$$\frac{\log_\epsilon \dfrac{W_1}{W_0}}{\log_\epsilon \dfrac{1}{k_1}}$$

A convenient way to lay off this slope is to draw it in a corner of the diagram as shown at AB. In order to fix T_c, it is only necessary to find two points on the curve separated the same distance and at the same slope as the hypotenuse of the triangle AOB. This is done for the example case as shown dotted in the diagram. The lower point at B' thus found on the curve corresponds to log T_1/T_c and since T_1 is fixed by the test conditions T_c can be calculated. This value can be checked by substitution in formula 10.

Graphical Presentation of Rating Data

After T_c is determined, the rating curve for any other impulse period T_2 can be laid out. The point corresponding to $k=1$ will lie at F, distant log T_2/T_c from the vertical axis of the diagram. Lines CF and GF form the axes for a new curve whose abscissa is proportional to log kT_2 and whose ordinates are proportional to log W_2/W_0 as shown on the diagram.

By tracing these curves on Figure 3 the curves shown are obtained. This gives a family of curves showing the permissible ratio of tube heating loss to loss at continuous duty at a number of values as a function of $kT=T_r$. It will be noted that when $T=T_r$ the W/W_0 ratio$=1$. At low values of T the curves are nearly straight lines, but at large values they become curved and are not spaced as far apart as for corresponding lower values of T. A value of T is reached (63 in the curve) where no further advantage is gained by long cooling periods except for low values of demand ($T=100$ in the curve).

The above curve pertains only to actual energy loss in the tube. This can be translated into current values by plotting the corresponding current values as transformed by curve Figure 4, which gives the loss ratio between continuous rating and demand current as a function of the ratio of the corresponding currents.

Curve Figure 5 gives the rating ratio curves as a function of T_r and T for the example value of $T_c=23.1$ seconds.

Comparison of Thermal Action of Model and Ignitron

In comparing the model with the actual ignitron, the following considerations are worthy of note:

1. The temperature variation of the model finds no counterpart in any temperature assumed by any part of the ignitron which is susceptible to measurement. The nearest thing might conceivably be the equivalent temperature of the mercury vapor in the tube. Since this cannot be measured, the equations are set up so as not to make a numerical knowledge of any temperature necessary.

2. The criterion is assumed to be that no duty cycle shall impose a higher value of equivalent temperature on the tube than that determined to be safe at continuous operation. This assumption requires considerably more investigation than the authors have been able to do. However, since the action of ionic devices is very rapid, it would seem reasonable that any effect on arc back due to the equivalent temperature could act in such a short time that instantaneous values of this temperature would be critical.

Summary

The above theory suggests a method of defining the intermittent rating of an ignitron tube through the use of a so-called time constant. This method results in a smooth transition from intermittent to continuous duty ratings. It is possible to adapt the time-constant concept to the calculation of permissible ratings for more complicated duty cycles than are considered in the present paper.

The authors hope to be able, in the future, to check the actual performance of tubes against the above theory, especially at values approaching the continuous current rating.

Modern Cathode-Ray Oscillograph for Testing Lightning Arresters

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THIS oscillograph was designed to meet the specialized needs of a group that uses the cathode-ray oscillograph as the principal tool in the study of transients in lightning-arrester development. The instrument is desirable for many purposes, especially those requiring very high writing speeds in connection with nonrepetitive transients where photographic records must be obtained. Other requirements of primary importance in the design were convenience of operation; accuracy of measurement of current, voltage, and time; accuracy of the timing; and linearity and rectilinearity of the plate pattern.

The cathode-ray oscillograph with the film in the vacuum was first described by Dufour,[1] and improvements have been described by numerous investigators such as Rogowski and his associates,[2] Burch and Whelpton,[3] Whipple,[4] Norinder,[5] to mention only a few.

General Description of the Oscillograph

The essential elements of any cathode-ray oscillograph include a cathode and anode operated in a vacuum to provide the electron beam; deflecting means; and a photographic film or fluorescent screen. The electrons from the cathode are accelerated to a high velocity by the cathode voltage, pass through an aperture in the anode, pass between the deflecting plates or coils, and impinge on the film or screen. The oscillograph described in this paper includes these elements and additional elements to make the instrument suitable for recording transients of extremely short duration. A focusing coil increases the intensity of the beam, trapping plates control its duration independently of the cathode voltage, and high-voltage circuits apply the cathode and beam-trapping voltages in the correct split-micro second sequence. An automatic regulator holds the oscillograph pressure at the desired value. The cathode voltage is held constant by a voltage regulator. These features that provide ease of control, fidelity of recording, and high writing speed are described more completely.

A view of the instrument is shown in Figure 1 and a diagram of the high-voltage circuits in Figure 6. The cold cathode is operated at 60 to 80 kv obtained from an impulse generator. The film chamber, deflection tube, and beam-trap tube are metal; both metal and glass cathode tubes have been satisfactory. The deflecting tube contains a pair of sweep plates to move the beam along the time axis and a pair of deflection plates to move the beam in a direction perpendicular to the sweep. The trap, consisting of three pairs of parallel plates, is placed between the anode and focusing coil.

Vacuum System

The vacuum system, Figure 2, was designed to maintain a pressure in the cathode tube sufficiently high to give a copious discharge, while the pressure in the remainder of the oscillograph is low enough to minimize scattering of the beam and fogging of the film.[6] This differential in pressure is provided by a controlled leak from the atmosphere to the cathode tube and by the restriction in flow of air from the cathode tube to the remainder of the oscillograph at the anode hole. The direction of air flow isolates the cathode tube from condensation-pump vapor without the necessity of a trap.

When the oscillograph is pumped down from atmospheric pressure, a rough vacuum is first obtained with the condensation pump by-passed through valve 5; valves 3 and 4 open, and valves 1 and 2 closed. To obtain a higher vacuum, valve 5 is then closed, and valves 6 and 7 are opened by a switch controlling the interlocked solenoids. The condensation-pump heater circuit and valve 5 will be opened and valves 6 and 7 closed by a flow switch if the cooling water for the condensation pump stops.

After the entire oscillograph has been evacuated to a low pressure, valve 3 is closed and valves 1 and 2 are manually adjusted to allow a gradual increase in the cathode-tube pressure. At this time, the vacuum system is switched to automatic operation with valves 3 and 4 controlled by a contact-making vacuum gauge to regulate the rate of leak to the discharge tube. Interlocked solenoids normally hold valve 3 closed and valve 4 open. When the pressure in the discharge tube has increased to the desired value because of the leak, the gauge causes the momentary opening of valve 3 and closing of valve 4, reducing the leak-chamber pressure. The ratio of expansion to leak-chamber volume is about two per cent, and the pressure in the leak chamber is much greater than in the expansion chamber so the rate of leak to the discharge tube is also reduced by about two per cent by the operation of valves 3 and 4. The vacuum gauge continues these automatic adjustments in rate of leak as frequently as is necessary, up to three times per minute, to maintain pressure in the cathode tube within a tolerance of 0.1 micron. Sudden changes in cathode-tube pressure are prevented by the smoothing action of the leak chamber.

Focusing Coil

The focusing coil has a steel shroud and pole pieces to give a short magnetic field of uniform intensity over the aperture to minimize aberration. The focusing current is held constant, independent of variation in line voltage, by a gas regulator tube. The effect of the focusing coil on the beam intensity is shown in Figure 3 by comparing the 5-millimeter width of the 1000-volt calibration line taken with the focusing coil unenergized, with the 0.5-millimeter width of the dense zero line taken with the proper focusing coil current. A fine trace can be obtained with no focusing coil provided the anode hole is very small, but this greatly reduces the writing speed.

Beam Trap

There are three pairs of cross-connected beam-trapping plates, as described by Burch and Whelpton.[3] With perfectly balanced plates, voltages insufficient to trap the beam will not deflect it. Actually it is impossible to adjust the plates perfectly; therefore, the trapping voltage is kept to a minimum during the sweeping time. Voltages in excess of about one kilovolt deflect the beam so that it cannot pass through an aperture located between the two upper pairs of trapping plates. The beam is cut off below the anode except during a relatively short interval adequate for sweeping it across the film, and conse-

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quently fogging of the film is reduced. With impulse excitation of the cathode, a beam trap is not a necessity as with continuous excitation, but it is advantageous to cut the beam off while the cathode discharge is stabilizing and after the beam has been swept across the film.

Cathode-Voltage Regulator

The charge on the capacitors of the impulse generator supplying the cathode voltage is made independent of the supplyline voltage by a regulator of the electronic type. If variation of cathode voltage is permitted, the deflection will vary as is shown in Figure 4 by the two 1,600-volt calibration lines differing by three per cent which were taken with the regulator disconnected. Two zero lines were also taken but coincide since variations in cathode voltage do not affect the position of the zero line.

Elimination of Spurious Deflection

Magnetic fields deflect the beam, but a constant field, such as that of the earth, merely affects the position of the zero axis and is not objectionable. The synchronous switch (see Figure 6) prevents random timing of the oscillograph operation with respect to the periodic variation in magnetic field from nearby 60-cycle apparatus. It prevents errors such as are shown in Figure 5 by the separation of 0.5 millimeters between two zero lines taken with the switch stopped.

The metal deflection tube in this oscillograph prevents errors such as may occur in oscillographs with glass deflection tubes due to charges transferred from the beam to the glass walls.

The deflection and sweep plates were designed and adjusted so that the deflection produced by the two sets is at right angles and is proportional to the voltage.

High-Voltage Circuits and Sequence of Operation

It would be impossible to obtain oscillograms of nonrecurrent transients of short duration without proper timing of the cathode-voltage and sweep circuits of the oscillograph, and synchronization with the transient to be studied.

A push-button control starts a sequence of operations in the high-voltage circuit of Figure 6. These operations include:

1. Charging the cathode generator.
2. Application of voltage to the cathode.
3. Trapping the beam while the cathode discharge stabilizes.
4. Sweeping the beam across the film.
5. Initiation of the impulse generator so that discharge occurs during the sweep.
6. Holding the beam off the film after the sweep is completed.
7. Grounding the high-voltage circuits.

In order to obtain constant sweeping speed, the sweep plates are operated at equal voltages of opposite polarity with respect to ground. For this reason, much of the high-voltage circuit is symmetrical with respect to ground. The operation of this circuit is best explained by considering each step in the order of occurrence.

Cathode-Generator Charging Period

Operation of the control button starts the charging of the cathode generator. The Marx circuit capacitors C_g and the reservoir capacitor C_1 are charged to 20 kv, negative, and a positive charge of 20 kv is placed on the similar capacitors C_g' and C_1'. Simultaneously, the sweep-supply capacitors C_6 and C_6' in circuit 5 charge to equal voltages of opposite polarity through resistors R_{16} and R_{16}'. A fraction of this voltage, determined by the ratios of R_{14} to R_{15} and R_{14}' to R_{15}' in circuit 4 is applied to the sweep capacitors C_8 and C_8' to bias the cathode beam. Also a fraction of the voltage on the supply capacitor C_6 is applied to the beam-trap plates by the high-resistance potentiometer R_{22}, R_{23}, in circuit 8, compensated by capacitors C_{13}, C_{14} for fast response. Capacitor C_5 in circuit 3 is charged to 40 kv.

Application of Voltage to Cathode

When the capacitors of the cathode-voltage generator have been charged to the proper value of about 20 kv, the volt-

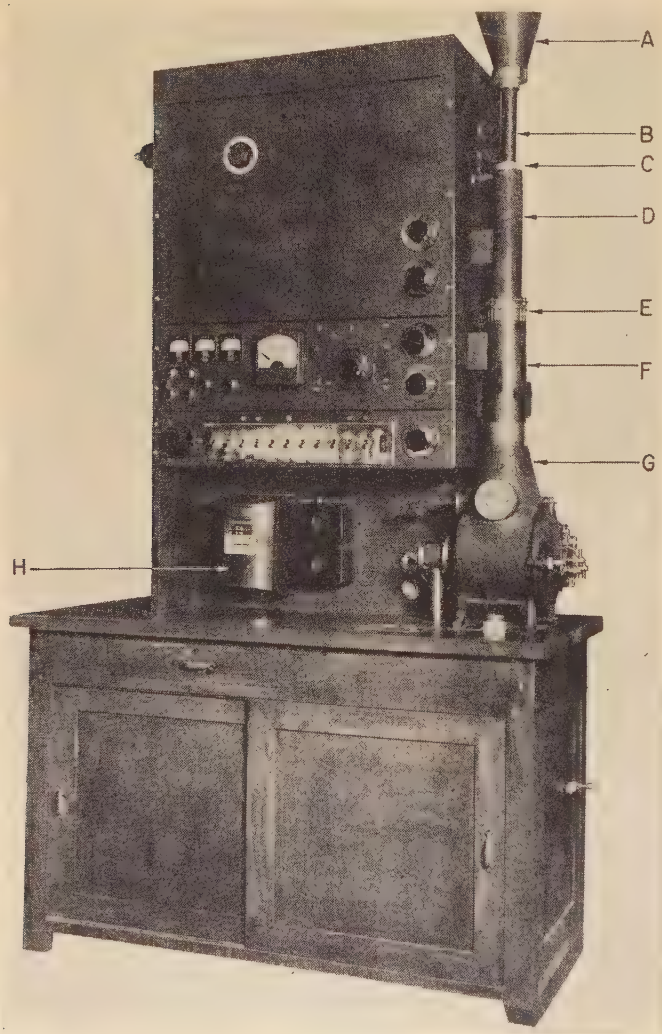


Figure 1. Oscillograph viewed from the front

A—To cathode generator
B—Cathode tube
C—Anode
D—Beam trap
E—Focusing coil
F—Deflection chamber
G—Film chamber
H—Vacuum-regulator gauge

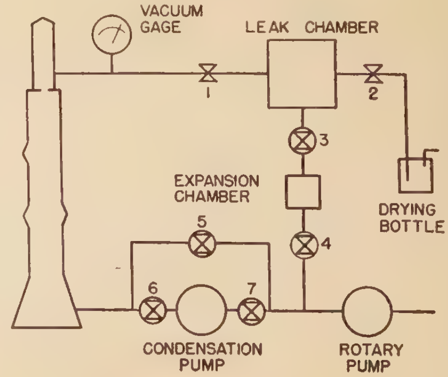


Figure 2 (right). Vacuum control system

Valves 1 and 2 are manually adjusted. Solenoid valves 3 and 4 are controlled by the contact-making vacuum gauge. Solenoid valves 5, 6, and 7 are interlocked

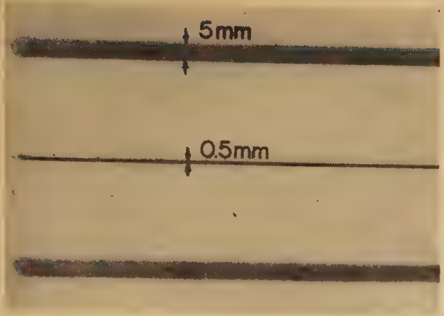


Figure 3. Effect of focusing

The focusing coil was unenergized for the 1,000-volt calibration lines, but was energized for the zero line

age regulator automatically opens the low-voltage charging circuit and releases the moving arms of the synchronous switch and the grounding contactor. The operating time of these switches is long compared with the longest sweeping time of the oscillograph. When the arms of the synchronous switch touch the middle pair of contacts, the cathode generator discharges as soon as the rotating arm reaches the closed position, which times the discharge with respect to the 60-cycle circuits. The gap G_1 discharges, and 80 kv negative is applied to the oscillograph cathode.

Gap G_2 discharges after an interval provided by the charging time of C_2 in circuit 1 for stabilization of the cathode discharge.

Initiation of the Impulse Generator

After G_2 sparks, gap G_3 is delayed by the time required to charge C_3 in circuit 2; the delay can be adjusted by the variable inductance. When G_3 sparks, an initiating impulse travels to the three-electrode gap of the generator[7] supplying the impulse to be measured. These circuits require careful adjustment to time the transient discharge to occur early in the period while the beam is crossing the film, which

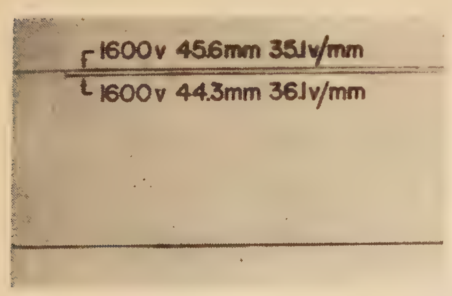


Figure 4. Effect of variation in supply voltage on deflection

Oscillograms taken with cathode-voltage regulator disconnected

Figure 5. Effect of stray magnetic fields

Two zero lines taken with synchronous switch stopped; separation is caused by difference in instantaneous value of magnetic field from 60-cycle equipment

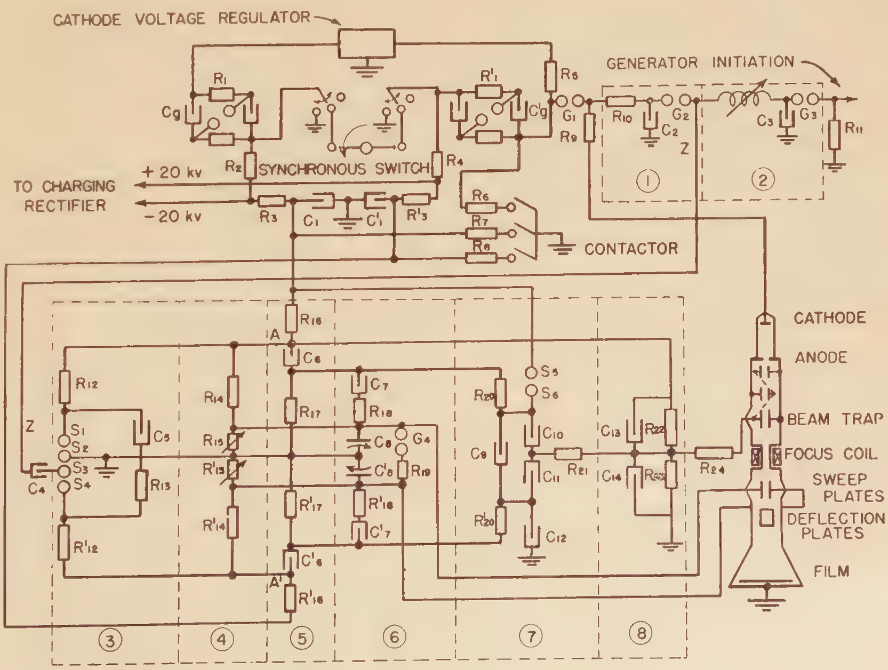
may be as short as 0.25 microsecond with a fast sweep.

Removal of Beam-Trap Voltage

The sparking of G_2 applies voltage to the line Z terminated by capacitor C_4 in circuit 3. This voltage disturbs the balance of voltage across the four-electrode gap S_1–S_2–S_3–S_4, which sparks and grounds electrodes S_1 and S_4 simultaneously. The capacitor C_5 discharges through R_{13} and the four-electrode gap, thus insuring low arc resistance and thorough grounding. A four-electrode switching gap, not previously used in such circuits, was necessary rather than the usual three-electrode gap. The two end gaps S_1 and S_4 must operate at equal voltages of opposite polarity to give the necessary symmetry of circuits; another electrode S_2 provides the connection to ground, and hence a fourth initiating electrode S_3 was introduced.

Figure 6. High-voltage circuits

1—Sweep delay
2—Impulse-generator delay
3—Grounding
4—Deflection-plate bias
5—Supply
6—Sweep
7—Beam cutoff
8—Initial beam hold-off



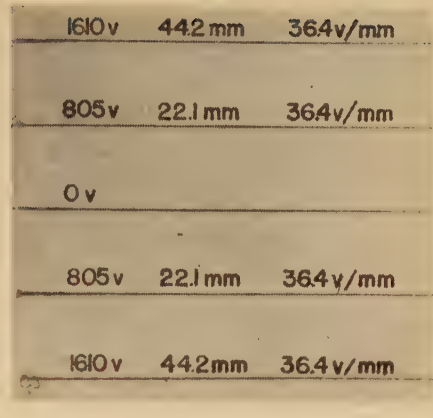


Figure 7. Consistency of deflection and absence of fogging

Five traces are superimposed on each of 5 lines. Superposition is possible because of the vacuum, cathode-voltage, and calibration-voltage regulators and the synchronous switch

The points A and A' in circuit 5 are grounded by the four-electrode gap. As a result, the beam-trap voltage is removed by the compensated potentiometer in circuit 8, and the beam appears between the deflection plates, but is held at the edge of the film by the charges acquired by C_8 and C_8' during the charging period.

Sweeping the Beam

Grounding of A and A' also puts 20 kv positive on R_{17} in circuit 5 and 20 kv negative on R_{17}' supplied from C_6 and C_6'. As a result, current flows through the blocking capacitors C_7 and C_7', resistors R_{18} and

R_{18}', and into sweep capacitors C_8 and C_8', which supply balanced positive and negative voltages to the deflection plates. This current reverses the sweep-plate bias voltage so that the beam is swept across the film. The plate voltage is limited by sparkover of G_4 after which R_{19} maintains sufficient voltage to hold the beam off the film.

Beam Cut-off

Voltage on R_{17} and R_{17}' also causes current to flow through R_{20}, C_9, R_{20}' in circuit 7 which builds up a positive voltage on S_6, while S_5 is held at 20 kv, negative, by the reservoir capacitor C_1. During the sweep when C_9 was charging, the voltage on the beam trap was unchanged, because circuit 7 is symmetrical and balanced to ground

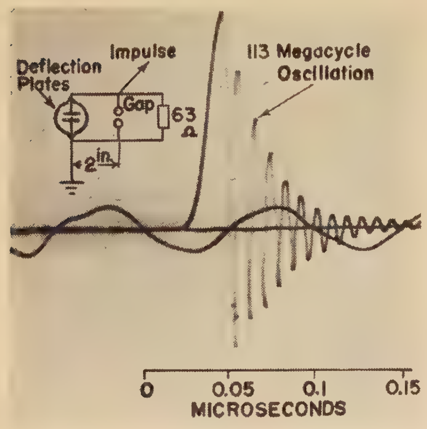


Figure 8. High writing speed

Voltage oscillation in a small circuit following gap breakdown. Timing wave is 10 megacycles; sweeping speed is 44 centimeters per microsecond and the maximum writing speed is 3,000 centimeters per microsecond

since C_{12} compensates for the capacitance of the gap S_5–S_6. After a time delay, the gap S_5–S_6 sparks, applying voltage from C_1 that unbalances the voltages on capacitors C_{10} and C_{11}. A fraction of this voltage appears on the beam trap through R_{21} and R_{24} and traps the beam.

High-Voltage Supply Grounded

The moving-contact arms of the synchronous switch have been falling continually since the holding contactor was tripped by the cathode-voltage regulator. When contact is made with the grounded stops, the cathode-generator capacitors C_g, C_g' are grounded. The grounding contactor was released also and when it closes, the other capacitors are grounded so that the operator can make adjustments in safety.

Operating Controls

Oscillator timing waves are provided by five pretuned circuits covering the range of 0.1 to 10 megacycles per second. Sweeping speeds which cross the film in 0.25 to 200 microseconds may be selected by means of a tap switch varying C_8 and C_8'. The maximum convenient sweeping speed is about 45 centimeters per microsecond. Special deflecting-plate switches change the circuits for recording the several functions such as the timing wave, calibration voltage, and impulse voltage or current. In operation, the desired conditions are set up by means of those switches, and the sequence of operations of the circuits to make the exposure is initiated by a single push button.

Performance of the Oscillograph

Several factors have been discussed that make the oscillograph free from spurious

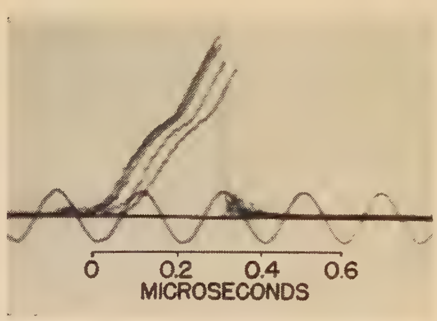


Figure 9. The consistent timing of oscillograph and generator is illustrated by 5 volt–time oscillograms of gap breakdown on one film. The variation in timing is 0.08 microsecond

deflection. Figure 7 shows a zero line and deflections due to positive and negative calibration voltages of 805 volts and 1,610 volts. This oscillogram demonstrates the accuracy and consistency of the instrument, since five separate traces were superimposed on each line without causing an appreciable increase in the width of the lines. The sensitivity was constant at 36.4 volts per millimeter for each calibration line. The 25 traces on the film have not caused fogging.

Figure 8 shows an oscillogram of a 113-megacycle oscillation in an extremely small local circuit following the breakdown of a gap. The beam crossed the film in 0.25 microsecond. The maximum writing speed on this oscillogram is about 3,000 centimeters per microsecond, which is ten per cent of the speed of light. To obtain this oscillogram, a rectangular circuit was connected directly to the deflecting plates, which provided most of the circuit capacity. The opposite side of the circuit contained a small gap that was sparked over by the impulse generator to start the oscillation. A resistor was connected in parallel with the gap to prevent deflection by voltage induced in the plate circuit previous to gap breakdown. Dividers are necessary for the measurement of high voltages and will limit the accuracy with which ultrafast transients can be recorded, rather than the writing or sweeping speeds of the oscillograph.

Comparison With Hot-Cathode Oscillograph Having Permanently Sealed Vacuum

The sealed-in high-speed oscillograph with the film outside the vacuum, on which improvements also have been reported[8,9] within recent years, occupies the position of an every-day general-purpose instrument for transient work. It is simpler in construction and operation and capably records all but the ultrafast transients for which the instrument described in this paper is especially valuable.

Conclusions

The oscillograph described should be regarded as a precision research tool capable of recording ultrafast phenomena. Due to the complexity of the circuits, the operator must use considerable care and skill to realize the best performance. The authors believe it represents marked progress in the design of ultrahigh-speed oscillographs for the measurements of transients and that several features add definitely to performance characteristics hitherto attained. During the two years in which this instrument has been in constant use, the accuracy, consistency, high writing speed, and convenience of operation have been of great benefit in the study of lightning arresters and problems of protecting insulation.

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6. Cold-Cathode Cathode-Ray Oscillograph

Application of Apparatus and Conductors Under Various Ambient-Temperature Conditions

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Synopsis: Various electrical standards are reviewed from the viewpoint of the selection and interpretation of ambient temperature values. Equivalent aging temperatures for insulation have been calculated from recorded ambient temperature data for several typical outdoor and indoor locations. From these the suitability of ambient values now used for the establishment of temperature rises for rating purposes is discussed. These values seem to be well chosen, but some clarification in meaning and in methods of using them for rating and application purposes is desirable. For apparatus rated on a 40 degrees centigrade ambient basis, appreciable margins in permissible temperature rise exist under many conditions and for many places of application. Permissible increases in loading for certain motors without exceeding conservative hot-spot temperatures are suggested, subject to the limitations imposed by other operating considerations.

Standards for Electrical Apparatus

THE permissible temperature rises for rating purposes given in various commercial standards for apparatus and conductors were obtained by considering the maximum continuous temperatures for insulation and selecting certain values of ambient temperature. The ambient values most commonly used are:

40 degrees centigrade—For commercial standards for the majority of all electrical apparatus and for some cable-rating purposes.

30 degrees centigrade—For commercial standards for wires and cables and, with qualifying statements, for the newly proposed standard for transformers.

25 degrees centigrade—For standards for rotating apparatus on electrical vehicles and in some Underwriters' regulations.

50 degrees centigrade—For standards for marine apparatus for which 40 degrees centigrade is not considered adequate.

Generally speaking, these values are well chosen for the various cases indicated, but there is some inconsistency between the wording of the standards and the methods and generally accepted theory of applying apparatus. Some standards simply specify that apparatus will have the proper rating when the temperature of the cooling medium does not exceed 40 degrees centigrade. In at least one case the statement "at no time exceeds 40 degrees centigrade" is used. In others 40 degrees centigrade is specified as the limiting ambient temperature of the cooling air or other gases, with a supplementary statement that the ambient temperature in locations where electrical apparatus is operated in the United States rarely exceeds 40 degrees centigrade. There is a general impression that 40 degrees centigrade is exceeded in only a few locations, but this is not confirmed by actual records. Weather bureau statistics show that outdoor temperatures have at times exceeded 40 degrees centigrade in 30 out of 60 stations.[3] Thus, in indoor applications, where considerable heat is generated by losses in large electrical apparatus, heat engines, steam pipes, and industrial heating equipment, 40 degrees centigrade will at times be exceeded in a large number of applications. However, the total percentage of time during which 40 degrees centigrade is exceeded is as a rule very low, not more than 0.5 per cent for most outdoor locations and a somewhat higher percentage for some indoor locations. Nevertheless, with a strict interpretation of the statement that the cooling medium must not exceed 40 degrees centigrade, a large percentage of applications would have to be considered as special. In actual practice, standard apparatus having a temperature-rise rating based on a 40 degrees centigrade ambient is actually applied more or less indiscriminately with success in all but unusually hot locations. This is to be expected in the light of various investigations on the life of insulation, which have shown conclusively that life or rate of deterioration is dependent upon both time and temperature.

Since the temperature-rise rating presumes continuous operation at the standard ambient temperature, it may be expected that satisfactory life of insulation at rated load will be obtained when this ambient temperature is maintained continuously. If this is true, it follows directly from these investigations on the life of insulation that equal life will be obtained in spite of short periods at temperatures above 40 degrees centigrade, if values below 40 degrees prevail for the greater part of the time. This is the condition found nearly everywhere in practice; ambient temperatures usually vary over wide ranges and include values below 40 degrees centigrade for most of the time.

On the basis that a continuous ambient is used for rating purposes, it is merely necessary to make sure that the prevailing ambient temperatures, which vary continually in practice, are equivalent in their aging effects to this standard value. This will result in the most effective application of apparatus. A method of determining the aging temperature equivalent to any given varying temperature is available in the eight degrees centigrade rule,[1,2] which states that the aging effect of temperature doubles for each eight degrees increase in temperature. This eight degrees centigrade rule is not a precise law and cannot be applied indiscriminately for all insulation, but it does serve as a useful guide for analytical purposes.

Equivalent Aging Temperatures

The use of the eight degrees centigrade rule is illustrated in Figure 1. Curve *A* was obtained from available outdoor temperature records for Dallas, Tex. It shows the percentage of time during which certain temperature values were exceeded. To obtain corresponding machine temperatures, the hot-spot temperature rise

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*For an extensive bibliography see pages 727 and 728 of reference 8.

of 65 degrees centigrade permissible for many forms of class-A insulation is added to curve A, giving curve B. By using the eight degrees centigrade rule, it is possible to calculate a constant temperature D (appendix II) which has the same effect in deteriorating insulation as curve B. By subtracting 65 degrees centigrade from D, the value C is obtained. This is a single value equivalent to the ambient temperature curve A. The validity of this operation depends, of course, on the empirical eight degrees centigrade rule, but its application is conservative. It is assumed that the insulation temperature follows changes in ambient temperature exactly. Actually, insulation temperatures lag and the effect of the higher ambient temperatures is somewhat less than curve B indicates.

Dallas is one of the hottest places in the country. Although the maximum temperature exceeds 40 degrees centigrade, the equivalent yearly outdoor temperature is only 22 degrees. Figure 2 shows

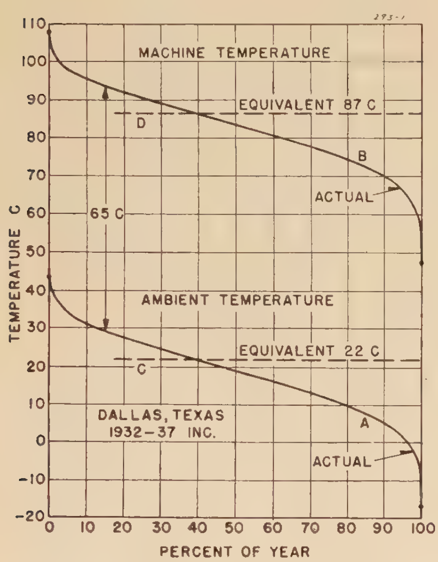

Figure 1. Actual and equivalent ambient and machine temperatures for outdoor locations in Dallas, Tex., 1932–37 inclusive

curves and values for other locations and the equivalent temperatures are from 12 to 22 degrees centigrade. Therefore, outdoor apparatus can be safely applied almost anywhere if the standard temperature rises are based on an ambient of 25 degrees centigrade. However, a further assumption that heat from sun radiation can be neglected is necessary. It has been shown that this assumption is correct in most cases, because wind velocities nearly always existing outdoors, though rather low, compensate for sun radiation. There are a few exceptions to this, such as meters or transformers mounted in well-protected corners of buildings and subject to considerable sun exposure, the influence of which may then at times be appreciable.

Most types of electrical apparatus are at times located indoors and the consideration of outdoor temperatures is in itself of restricted value. However, it is useful as a basis for the study of indoor temperatures for which no comprehensive statistics are available or likely to be available in the near future. Therefore, a logical method is to use outdoor statistics and make tests on the differences between outdoor and indoor temperatures for a limited number of typical indoor locations.

In buildings heated for human comfort, a more or less constant temperature is maintained during the cooler seasons of the year. A constant value of 25 degrees centigrade represents the higher limit of temperatures prevailing during the heating period. At other times it might be assumed that the indoor temperature can be maintained the same as the outdoor temperature in a substantial ideally ventilated building. In practice there are, however, a number of factors tending to increase indoor temperatures even when no appreciable amounts of heat are generated inside. These factors are: heat from occupants and lighting fixtures, the poor heat exchange generally given by open windows, the fact that windows are kept closed a part or all of the time, and, finally, heat caused by sun radiation.

Figure 3 shows simultaneous outdoor and indoor records taken in an office in a substantially built modern office building having northwestern exposure with sun radiation through the windows only after 5 p.m. At the end of the office hours, about 5 p.m., the windows are closed and night temperatures are appreciably higher indoors. During the second day shown it was cloudy after 5 o'clock, and the maximum temperature was only slightly higher indoors. In contrast, the marked influence of the late afternoon sun appeared during the third day, resulting in a difference of 5.6 degrees centigrade (ten degrees Fahrenheit) at 6:30 p.m. Similarly the influence of sun exposure with closed windows, as may be found in unoccupied storerooms and similar places with southern exposure, can be appreciable. The high peak on the first day indicated in Figure 3 was caused by the recording instrument being directly exposed to the sun. The effect is quite marked, although the instrument case is light in color and ventilated. If apparatus with dark heat-absorbing surfaces is subjected to sun radiation through windows, the effect may be quite marked because of the lack of compensating outdoor breezes.

Similar records have been obtained in a powerhouse basement and a factory building. The powerhouse basement was without windows and poorly ventilated, but the recorder was at least ten feet from direct heat sources such as steam pipes. The factory building was a single-story structure, with a covering of boards plus sheet iron on the walls and paper on the roof. Several small ovens operating at temperatures up to 175 degrees centigrade were located in the vicinity of the thermometer. It was placed near the south windows as well, which resulted in considerable influence from sun radiation.

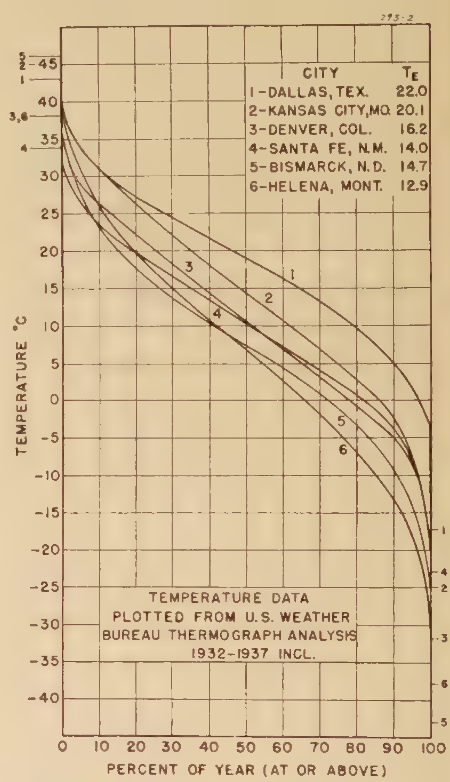

Figure 2. Actual and equivalent outdoor ambient temperatures for six cities of the United States

From these tests the curves of Figure 4 have been estimated for certain indoor conditions and the equivalent ambient temperatures calculated. The tests covered only a 100-day period in midsummer, but the data have been adjusted to an annual basis compared to the outdoor curve for the period of test and then applied to the Dallas outdoor curve. A constant indoor temperature of 25 degrees centigrade has been assumed when lower temperatures prevailed outdoors. Figure 5 shows the same procedure applied to the outdoor curve above 25 degrees centigrade for Bismarck, N. D. This is one of the hotter locations in the northern section of the United States, as divided in Figure 2 of AIEE Standard No. 1.[3] Although maximum temperatures are high, they do not persist as in more southern climates, and

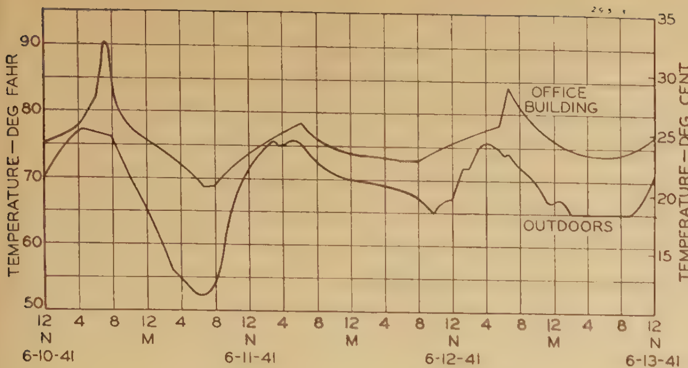

Figure 3. Recorded temperatures outdoors and in a modern office building at East Pittsburgh

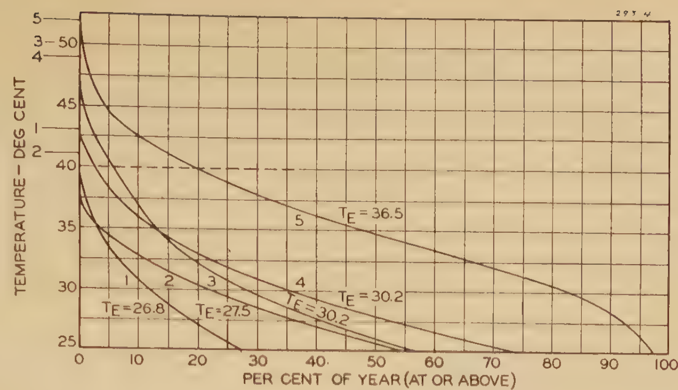

Figure 4. Estimated prevailing and equivalent ambient temperatures for typical locations in Dallas, Tex.

1. Outdoors 1932–37 inclusive—above 25 degrees centigrade
2. Office building—northern exposure—open windows daytimes
3. Office building—southern exposure—closed windows
4. Building with industrial heat sources—thin walls
5. Powerhouse—poorly ventilated area

equivalent aging temperatures are correspondingly lower than in Dallas. The equivalent aging temperatures for these selected locations in Dallas and Bismarck lie between 25 and 32 degrees centigrade for the most part, with the Dallas powerhouse 36.5 degrees centigrade. In the latter case, even with a 25 degrees centigrade minimum temperature, 30 degrees exceeded most of the time, and 40 degrees exceeded 20 per cent of the time, the equivalent temperature is still well within the 40-degree standard. In practice all sorts of variations will be encountered in individual applications, but for either locality it seems that, regardless of high maximum temperatures, equivalent aging temperatures as high as 40 degrees centigrade are likely to be found only under very unusual circumstances. Since Dallas is one of the hotter places in the country this statement may be applied to apparatus locations in buildings generally.

Figure 5 seems to indicate that, except with a great deal of heat generated and poor ventilation, the equivalent aging temperature will rarely exceed 27 degrees centigrade for large sections of the country. This applies particularly to many highly industrialized sections of the United States. Moreover, the 27 degrees centigrade value is based on the assumption that indoor winter temperatures are 25 degrees. Actually, the temperatures maintained in industrial establishments in winter are nearer 20 degrees centigrade, which results in equivalent aging temperatures for the year certainly no greater than 25 degrees.

Enclosures and Buildings

The inside temperatures of enclosures provided for the specific purpose of housing electrical apparatus are usually higher than the outside. In relatively small enclosures with restricted ventilation, such as steel cubicles for switch-gear, a distinction is made between inside and outside ambient temperatures. Extreme differences have of course been encountered when the enclosure had no ventilation. These conditions require special consideration in each case. On the other hand, when apparatus is enclosed in a regular building, the temperature in the building is considered the ambient temperature for the apparatus.

In this connection some interesting tests have been reported on an indoor transformer installation.[4] A ventilated building about 18 by 25 feet and 24 feet high housed three power transformers. In Figure 6 the heaviest lines represent the outline of the building, with cooling air flow indicated by the arrows. Figure 6a shows isothermal lines midway between two transformers, and Figure 6b shows the isothermal lines for a section through the center line of a transformer. The numbers given on the lines indicate the difference between outside and inside temperatures. The average temperature in the building is about eight degrees centigrade above the outdoor temperature. In the case of Dallas this would give an equivalent temperature of about 30 degrees centigrade. However, the lines above the transformer show an excess of 11 to 15 degrees centigrade over outside temperature. Applied to the Dallas equivalent temperature of 22 degrees centigrade, this gives ambient values of 33 to 37 degrees. If auxiliary apparatus were installed above the transformers, it would be operating under these ambient conditions. This conclusion is made under the assumption that the building is fully ventilated at all times; however, the ventilation may be restricted in cooler seasons of the year, resulting in equivalent ambient temperatures of 35 to 40 degrees centigrade at points above the transformers. It is believed that this general situation is frequently found in buildings housing electrical apparatus.

A somewhat different case is an industrial building subject to considerable sun radiation or other heat sources. Temperatures in the upper regions of the building, where some types of apparatus may be installed, may be appreciably higher than in the lower parts where temperatures usually are measured. Although some of the previous cases derived from tests do not show equivalent ambient temperatures in excess of 35 degrees centigrade, equivalent temperatures close to 40 degrees may be expected in some parts of many buildings. The short-time maximum temperature in such locations is likely to be 50 to 60 degrees centigrade.

Preferred Standard Ambient Temperatures

From these studies, the suitability of the ambient temperature values used for temperature-rise standardization purposes can be considered. For maximum simplicity in the application of apparatus, the ambient temperature of 40 degrees centigrade has been well chosen. The temperature conditions for the very small percentage of cases where apparatus so standardized cannot be safely applied are so unusual that they will be readily recognized. (An equivalent temperature of 40 degrees centigrade usually permits occasional maximum values of 50 to 60 degrees; consequently, misapplications are easily avoided.)

On the other hand, it might be contended that 40 degrees centigrade is contrary to maximum economy, because in a large percentage of applications equivalent ambient values of 30 to 35 degrees are not exceeded. The question of what would be gained by using 35 degrees centigrade as a

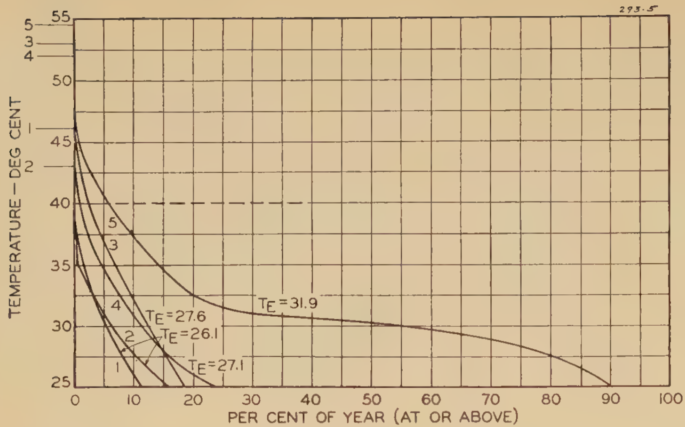

Figure 5. Estimated prevailing and equivalent ambient temperatures for typical locations in Bismarck, N. D.

1. Outdoors 1932–37 inclusive—above 25 degrees centigrade
2. Office building—northern exposure—open windows daytimes
3. Office building—southern exposure—closed windows
4. Building with industrial heat sources—thin walls
5. Powerhouse—Poorly ventilated area

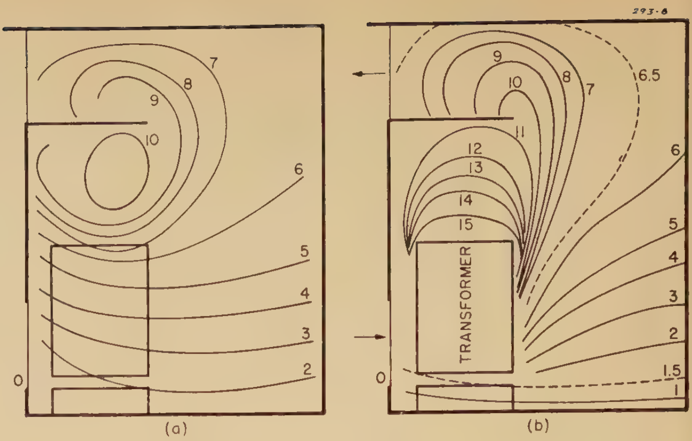

Figure 6. Isothermal lines around a large transformer in a ventilated enclosure

(*a*) Between two transformer units
(*b*) About the center line of one unit

basic ambient value might therefore arise. It would permit hot-spot temperature rises[3] of 70 instead of 65 degrees centigrade for class-*A* insulation, and 95 instead of 90 degrees for class-*B* insulation, gains of 7.7 and 5.6 per cent respectively. With apparatus having chiefly copper losses, this means increases in rating of only 3.9 and 2.8 per cent respectively; with apparatus having both iron and copper losses, gains of 7 and 5 per cent might be realized.

For these gains it seems wrong to sacrifice the present satisfactory situation which permits almost indiscriminate application of standard apparatus drawn from storerooms all over the country. A great deal of this apparatus has line-voltage magnet coils, and adjustment of the load to suit special ambient temperature conditions is not possible; the same is true with motors permanently built into or connected to driven machines. In these cases special apparatus would be required, if the temperature rating of standard apparatus were not based on a sufficiently high and universally applicable ambient temperature. The cost probably would more than balance any economic gain from a lower ambient as a basis for standard apparatus. It is of course possible to load certain machines somewhat below their normal rating, if the ambient temperature is higher than that used as a basis for the rating; however, it is just as easy to apply a machine to a somewhat higher load, if its rating is based on 40 degrees centigrade, and it is used in a locality with a lower equivalent ambient value.

Considering further that there are certain inaccuracies in the eight degrees centigrade rule used as a basis for this study, and that there is usually not much known about the temperature conditions for many applications, it certainly seems advisable to retain the margin of safety and the simplicity afforded by the use of 40 degrees centigrade as an ambient value for general purposes. A further reason for adhering to this value is that frequently apparatus is installed under conditions which interfere with proper ventilation.

In Figure 7, curve *A* has been estimated to show the percentage of all apparatus that can be applied without special consideration for temperature-rise ratings based on different standard ambient temperatures. In the absence of extensive statistical data no claims can be made for the accuracy of the curve. It does illustrate the basic considerations involved and the greater applicability of stock apparatus, the higher the standard ambient temperature on which ratings are based.

In spite of the advantage of the 40 degrees centigrade value for general use, it must be admitted that the use of lower values such as 30, or even 25 degrees, is justified where some conditions of application or economic considerations apply. One such case is rubber-insulated wire, some varieties of which are suitable for a continuous maximum temperature of only 50 degrees centigrade. The permissible rise is 20 degrees centigrade with an ambient temperature of 30 degrees, and only 10 degrees with an ambient of 40 degrees; in other words, the increase of rating possible with 30 degrees is 41 per cent. The greater part of this wire is used in places where an equivalent temperature of 30 degrees centigrade is not exceeded. This is roughly illustrated by curve *B* of Figure 7. (The eight degree centigrade rule may not apply to rubber-insulated wire, and curve *B* may not be quantitatively correct.) It follows that the sacrifice in economy resulting from an ambient of 40 degrees centigrade for this wire is entirely too great for the advantages gained thereby. This is especially true, because in the few locations where higher temperatures prevail, standard wire can be used merely by reducing the rating. Hence, the storeroom problem, which would complicate the situation in connection with many other types of apparatus, is of no particular importance. On the whole, it seems that ratings for rubber-covered wire based on a standard ambient of 30 degrees centigrade are well justified.

Another case of interest is that of transformers. Curve *C* has been estimated as applying to transformers and shows approximately the percentage of transformers which can be applied on a 30-degree centigrade basis; this percentage is high because most transformers are located outside. A rather small percentage of 30 degrees centigrade transformers require special consideration in their application. Also, since transformers are nearly always applied by engineers fully familiar with all the problems involved, ratings based on a 30 degrees centigrade ambient seem well justified. (The proposed transformer standards specify that nominal rating can be used if the average temperature during any 24 hours does not exceed 30 degrees centigrade, with a maximum of 40 degrees, which gives a continuous hot-spot temperature of 95 degrees. This provision in itself will result in an equivalent value somewhat in excess of 30 degrees centigrade for the 24-hour period, as here developed. However, since there will be only a few days during the year with such high temperatures, it is estimated that the provisions in the transformer standards are about the same as an equivalent temperature of 30 degrees centigrade or less.)

Curve D applies to rotating apparatus for electric vehicles. In view of the necessity to keep weight to a minimum, and because such apparatus is nearly always outdoors, the present practice of a 25 degree centigrade standard ambient is justified. For the sake of maximum simplicity in the standard structure, a value of 30 degrees centigrade for railway work seems preferable and would hardly interfere with maximum economy. The five degree centigrade difference represents a very small percentage of the customary temperature rises in this type of apparatus.

There are other cases where a standard ambient temperature of 30 degrees centigrade would be justified. For example, some domestic or office appliances are used almost exclusively in locations where an equivalent temperature of 30 degrees centigrade is not exceeded while the devices are in use. There are practically no homes or offices in which the equivalent temperature exceeds 30 degrees centigrade while occupied. Even if higher temperatures prevail when windows are closed, this is of no particular importance because the devices are not then loaded and will not overheat.

Therefore, considering both simplicity and economy, it seems well to retain the 40 degrees centigrade value but to recognize that a 30-degree value is justified for some apparatus and applications. It will of course also be necessary to use the 50 degree centigrade value for those few locations, such as holds of ships, where 40 degrees is not sufficient. An even higher standard ambient value may be necessary in special cases.

The previous considerations all are based on the deterioration of the conventional insulating materials, with the idea of assuring satisfactory life of apparatus from that point of view. The influence of temperature conditions on the oxidation and operation of contact surfaces or upon the operation of electronic devices and other types of apparatus may, of course, be subject to different laws and thus require separate consideration. Very few quantitative data seem to be available for such studies, but AIEE standards coordinating committee 7 is working on these problems. Regardless of the results, it would seem best to use the same basic ambient values for standardization purposes, with the understanding that satisfactory results are obtained if these ambient temperatures are maintained continuously. Any difference between the characteristics of other types of apparatus and ordinary electromagnetic apparatus can be taken care of in the methods of application.

Conclusions

Ambient temperature values equivalent to the continually changing temperatures encountered in practice have been derived for various localities. From these data it is concluded that the standard ambient temperature values selected for use in establishing temperature rises for rating purposes in the various standards are, in general, sound. It is suggested, however, that some clarification in meaning and in methods of using these selected values for rating purposes is desirable.

The data also show that for apparatus rated on a continuous 40 degree centigrade ambient basis, certain margins in temperature rise exist under many conditions and for many places of application. Recognition of this situation is particularly valuable at this time, when it is often necessary to increase loads on industrial equipment. Under these rating conditions an equivalent ambient temperature of 25 degrees centigrade may permit overloads which increase operating temperature-rises by 15 degrees without exceeding permissible hot-spot temperatures, when this is compatible with other operating considerations.

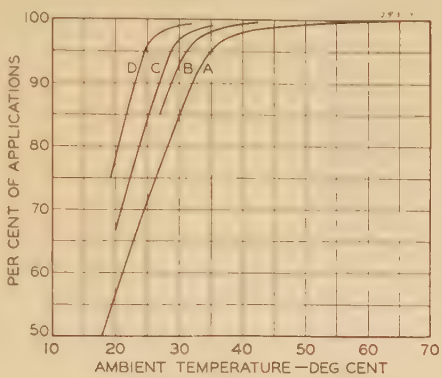

Figure 7. Estimated curves for different apparatus applications, showing the percentage of applications for which standard apparatus based on different ambient temperatures is suitable

A. General
B. Cables
C. Transformers
D. Railway apparatus

Apparatus ratings primarily are based on a continuous hot-spot temperature of 105 degrees centigrade for class-A insulation, although transformer standards specify 95 degrees. At times doubt has been expressed that the 105 degrees centigrade temperature will give satisfactory life of insulation, since laboratory tests have demonstrated loss in mechanical strength of materials continuously subjected to 105 degrees. On the other hand, hot spots in many machines occur in slots where the insulation is well supported and where some loss of mechanical strength can be tolerated. Extensive trials on actual machines at 105 degrees centigrade for long periods have shown very satisfactory life.

To eliminate this doubtful point, it can be assumed that the equivalent hot-spot temperature should not exceed 100 degrees centigrade. This takes advantage of only ten of the 15-degree margin and gives a permissible temperature-rise of 75 instead of 65 degrees, or an increase of 15.4 per cent. For general-purpose motors based on a 40 degree centigrade temperature-rise by thermometer, an extra ten degrees is available, permitting an increase in this case of 36.5 per cent. (Actually, the service factor for these machines requires the ability to carry a 15 per-cent overload with a 40 degree centigrade ambient maintained continuously.) Table I shows permissible increases in loads for motors, considering temperature only and taking advantage of these temperature-rise increases of 36.5 and 15.4 per cent. With the total losses increased by these percentages, permissible increases in copper losses are given in columns 3 and 5. Columns 4 and 6 show the increases in currents corresponding to these copper losses.

Table I. Permissible Per-Cent Increases in Motor Losses and Currents for 100C Hot-Spot Temperatures at 25C Ambient

Assumed Loss Distribution		General Purpose 40C Rise by Thermometer		Ratings Based on 50C Rise by Thermometer	
Iron	Copper	Copper Loss	Current	Copper Loss	Current
0.25	0.75	49	22	20.5	10
0.35	0.65	56	25	25.5	12
0.50	0.50	73	31.5	31	14.5

Since hot-spot temperatures are not directly proportional to total losses but depend also upon distribution of losses between copper and iron, these figures may be somewhat optimistic. It does appear reasonably safe, however, to increase currents on 50-degree-rise machines by 8 to 13 per cent and on general-purpose 40-degree-rise machines by 20 to 30 per cent under the conditions stated. For induction motors torque usually increases somewhat faster than the current. It is assumed, of course, that operation is at rated voltage. If operation is not at rated voltage, part of the margin indicated may not be available for increased loads.

Furthermore, this paper deals only with the effect of temperature on the aging of electrical insulation. There are other factors that should be considered when con-

templating placing an overload upon a rotating machine.[5] Mechanical strength of shafts, bearings and couplings, commutators, lubrication, and so forth, must be recognized as limiting factors also and should receive due consideration. Furthermore, utilizing these temperature margins may reduce insulation life from that commonly obtained under operation at substandard temperatures.

Unfortunately, simple conclusions covering all conditions encountered in practice are not possible, but it is hoped the data presented will be of assistance in determining increased loads for existing machines and apparatus in this time of emergency. Also, the statistical data of AIEE Standard No. 1 should be helpful in determining the localities where prevailing ambient temperatures might make increased loading of equipment possible.

Although the data given here are primarily intended to be helpful under present emergency conditions, it is also hoped that the views presented will be convincing evidence of the futility of suggestions for the use of a great variety of ambient temperature values and departures from standard temperature-rises in commercial transactions. Certain variations in ambient temperatures always will occur but wide confusion would result from attempts to reflect these into the system of rating apparatus.

Appendix I. Ambient Temperature Specifications in Standards

(The wording referring to ambient temperature in the different standards is far from uniform. The letters in these data indicate certain general classifications but do not cover all the variations in wording even where no distinctions in meaning are intended. Furthermore, where apparatus may be water-cooled, supplementary statements are given.)

	50C	40C	30C	25C	24C
AIEE and American Standards Association					
Metal-tank mercury-arc rectifiers		A			
Industrial control apparatus		B			
Electric-railway control apparatus		B			
Capacitors		B			
Apparatus bushings		C			
Air switches and bus supports		B			
Relays associated with power switchgear		A			
Protector tubes		A			
Fuses above 600 volts		B			
Automatic stations		B			
Switchboards and switching equipment		B			
Switchgear assemblies		A			
Lightning arresters		A			
Electric-arc-welding apparatus		B			
Resistance welding apparatus		B			
Electrical installations on shipboard	A	—			
Railway motors		—	—	B	
A-c power circuit breakers		B			
Rotating electrical machinery		B			
Transformers, regulators, and reactors		D			
National Electrical Manufacturers' Association					
Industrial control		B			
Motors and generators		A			
Oil circuit breakers		E			
Signalling transformers		—	—	—	F
General-purpose specialty transformers		E			
Underwriters Laboratories					
Industrial control		A			
Motor-operated fans		—	—	C	
Branch circuit and service circuit breakers		—	—	H	
Service cables		—	—	—	F
Auxiliary breakers					I
Motor-operated appliances					G
Insulated Power Cable Engineers Association					
Cables		A			
National Electrical Code					
Conductors		—	J		

General Classification

A. Equipment conforming with these standards shall be suitable for carrying the rated load when and where the ambient air temperature at the equipment does not exceed ——C.

B. Apparatus conforming with these standards shall be suitable for operation at the standard rating when and where the temperature of the cooling medium does not exceed ——C.

C. Apparatus bushings conforming to these standards shall be suitable for operation at their standard ratings when and where the temperature of the external cooling medium does not exceed 40C maximum or 30C average for any 24-hour period.

D. Apparatus conforming to these standards shall be suitable for operation at rated load with rated secondary voltage, provided the temperature of the cooling air at no time exceeds 40C, and the average temperature of the cooling air during any 24-hour period does not exceed 30C.

E. The standard ambient temperature of reference when the cooling medium is air shall be 40C.

F. The standard ambient temperature shall be 24C.

G. It is assumed that the ambient temperature will be ordinarily about ——C.

H. The temperatures given in these requirements are based on a room temperature of 25C.

I. The tests shall be conducted with the switch at room temperature, preferably 24C.

J. Capacities are based on room temperature of 30C.

Appendix II. Calculation of Equivalent Aging Temperature by Eight Degree Centigrade Rule

The continuous temperature equivalent to a given temperature distribution curve for a period of time is calculated by dividing the curve into convenient trapezoids or rectangles and applying equations 1 and 2.

$$A = t_0 \frac{(e^{KT_2} - e^{KT_1})}{K(T_2 - T_1)} \text{ for a trapezoid} \qquad (1)$$

$$A = t_0 e^{KT} \text{ for a rectangle} \qquad (2)$$

A = aging units
t_0 = time in convenient units (hours or per cent)
$e = 2.718$ $\qquad K = 0.0865$
T_2 = maximum temperature of trapezoid in degrees centigrade
T_1 = minimum temperature of trapezoid in degrees centigrade
T = constant temperature of rectangle in degrees centigrade

The aging units for all the sections of the curve are added, and equation 2 is applied to this sum for the total time involved. Solving for T gives the equivalent aging temperature for the curve.

With $K = 0.0865$ the aging units are doubled for an eight degrees centigrade increase in temperature.

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Effect of Lightning on Thin Metal Surfaces

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Synopsis: In the search for means for measuring the properties of natural lightning much can be learned from the evidence left at points struck by lightning. This paper deals principally with such evidence and the process of evaluating the characteristics of the lightning strokes responsible for the evidence.

For six years a nickel-plated, 18-inch copper sphere, 878 feet above the ground atop the WSM radiator at Nashville, had been collecting data in the form of holes and pits due to lightning strokes to the sphere. A total of 150 holes of varying sizes were found together with 300 pits. A laboratory setup was made consisting of the high-capacity impulse generator together with a d-c generator so arranged that the known characteristics of lightning with respect to the so-called continuing current could be duplicated. With this equipment holes having the same appearance could be produced in copper sheets, and from the results of the test a calibration curve was produced. At the same time calibration curves were obtained for other metals in addition to copper. From these data an expectancy curve was obtained between coulombs and per cent of holes. The average hole corresponded to 15 coulombs while the maximum size hole corresponded to 240 coulombs.

No satisfactory calibration was obtained for the pits, many of which no doubt were the result of high current peaks having a low coulomb content.

It is interesting to learn that of the 150 holes in the sphere 89 were found in the upper half and 61 in the lower half, with the great majority appearing in a six-inch belt around the seam at the sphere's equator.

The results of some laboratory high-current impulses on metal sheets are also shown, indicating pressure effects without appreciable burning.

A lightning stroke to a metal-roofed rural home is described, the largest hole in the roof indicating a charge of 210 coulombs. The home was not wired, but a part of the lightning discharge traveled a distance of 162 feet to the wired house next door to puncture the cellar wall and contact the neutral ground rod driven in the cellar. Another channel was found extending to the base of the service pole where two ground rods were driven. It was estimated that the discharge removed approximately 250 cubic feet of earth in its travel. Available data indicate the stroke was negative and had a total charge of more than 240 coulombs with a suggested peak current of the order of 200,000 amperes. Although the struck home was quite badly damaged, there was no fire, nor was there any trouble with the electric equipment in the wired home.

The results of this work indicate that sheet metal may be made to yield certain data of value with reference to lightning.

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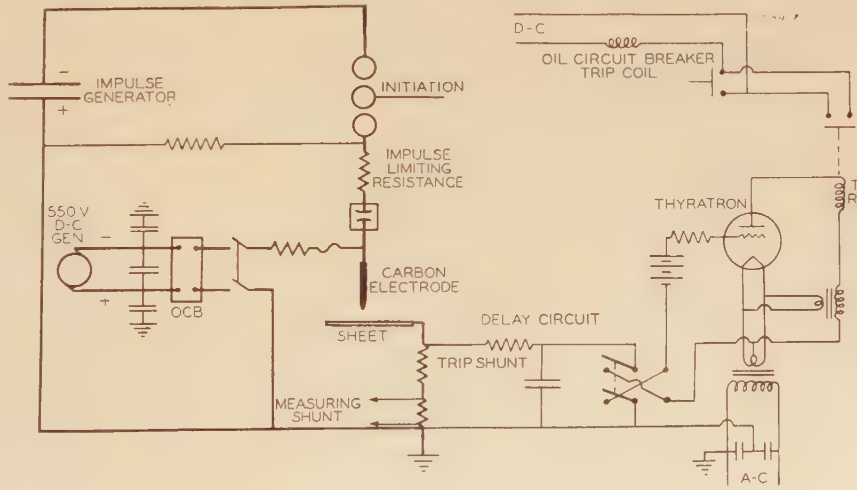
LIGHTNING strokes to objects on the earth have been characterized by two major effects[1–4] one causing explosive effects[5,6] and the other often resulting in fire. In many cases both effects have been present. The explosive effects are now known to be due to sudden increases in current which may reach a crest of several thousand or even a hundred thousand amperes or more in a few millionths of a second, decaying to half of the crest value in a time of the order of 40 microseconds. It is the expansion effects in the air resulting from these rapid current changes which cause the thunder which we hear. The burning effects are the result of the flow of current of relatively low magnitude of the general order of 200 or 300 amperes[7] for times which may be as long as 1.5 seconds, but on the average, persist for about 0.3 second or about 7,000 times as long as the average time to half value of the current peak. Correlating photographic and oscillographic data for strokes to the Empire State building having a height of 1,275 feet, strokes began at the building in about 80 per cent of the cases. As the height of the earth object becomes less, the percentage of cloud-initiated strokes increases. It is believed that for transmission-line heights the contacting strokes are always cloud initiated. Currents in earth-initiated strokes measured at the earth end begin with a relatively small value of current—a few hundred amperes—which develop into a continuing current when contact is made with the cloud. Superimposed current peaks may develop later, with varying time intervals and magnitudes, all of these peaks being cloud-initiated just as in the case of the stroke to objects of ordinary height on the earth's surface.

Laboratory Lightning

The impulsive nature of lightning strokes has been known for a long time, and it was natural to consider the cloud as one pole of a condenser and the earth as the other. Thus the laboratory lightning maker noted the similarity between the discharge of a charged laboratory capacitor and natural lightning. It looked the same, and the sound effects were there also. Furthermore, wood could be blown apart as in nature, and many of the distance–time–voltage or current phenomena found in connection with the operation of outdoor circuits could be reproduced. And so with the development of the Marx-circuit impulse generator and the cathode-ray oscillograph, data became available proving the similitude of the laboratory and the natural lightning as to the nature of the phenomena involved.

The continuing part of the natural lightning stroke was not simulated, as its existence was not shown oscillographically until 1937. The lack of knowledge of this component explains the inability of investigators to make the lightning fulgurites found in nature when discharging a lightning generator into wet sand. The impulsive nature of the current blew the

Figure 1. Circuit to control duration and amplitude of long-duration low-current discharges, preceded by impulse current



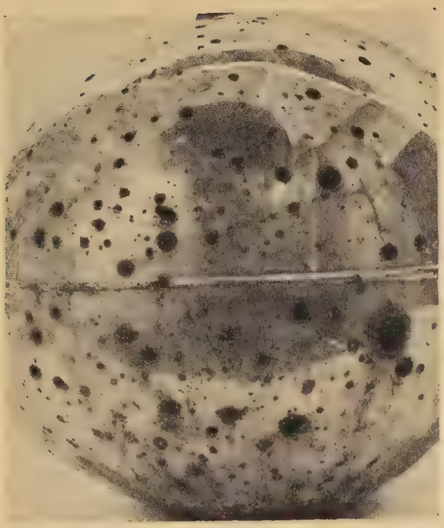


Figure 2. Side view of lightning-damaged 18-inch nickel-plated copper sphere installed for six years at top of WSM vertical radiator 878 feet above ground

sand out of the container, and it was not until currents of a few amperes were used, continuing for a second or so, that fulgurites could be formed.

The charge represented by the current peak is very small indeed. The average measured in natural lightning is only a fraction of a coulomb, whereas the charge represented by the continuing current may be of the order of 200 coulombs or more. Even very large impulse generators with voltages suitable for general testing work will have a charge of considerably less than ten coulombs. It is clear, therefore, that to simulate the complete lightning stroke a generator of more than 20 times the present capacity is needed, and therefore it is necessary as an economic expedient to make use of generators such as illustrated in Figure 1, where the discharge is initiated by a high capacitance generator, with a follow current supplied by a 500-volt d-c generator. Such an arrangement, though not suitable for long arcs, was quite satisfactory for the tests on metal sheets described in this paper. Modifications of this circuit have been used during other long-duration lightning current tests. More than one high current impulse may be superimposed using a circuit of this general character.

Lightning Strokes to Sphere at WSM

Our interest in the use of thin metal as a means of collecting lightning data was aroused when William Montgomery, Jr., engineer for WSM radio station of the National Life and Accident Insurance Company at Nashville, Tenn., sent us the sphere illustrated in Figure 2. This nickel-plated, 18-inch diameter sphere made of 19-mil copper had been in service at the top of WSM's vertical radiator, 878 feet in height, for a period of six years. During that time it was struck frequently by lightning and in the summer of 1937 was thrown to the ground during a lightning storm which damaged the tuning coil at the base of the tower as shown in Figure 3. The sphere was damaged in its fall on the reverse side but had been mounted in the position shown in Figure 2.

The large number and varying size of holes and pits found indicated that the sphere could be made to tell a story about lightning strokes if a proper calibration for the holes could be found. The globules of copper found around many of the holes indicated clearly that the holes were produced by continuing currents rather than by current peaks, which would have produced sufficient pressure in many cases to have blown the molten metal away.

Tests were begun, therefore, with the circuit shown in Figure 1, using a variety of current and time values until the appearance of the holes produced corresponded closely to those found on the WSM sphere. A few of the test holes are reproduced in Figure 4. A calibration curve is shown in Figure 5 together with a similar curve for galvanized iron. All tests were made with the sheet positive, electrode negative.

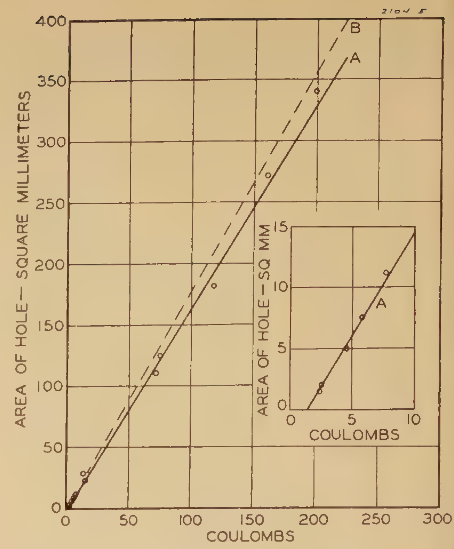


Figure 5. Relation between coulombs in the arc and resultant area of holes burned in (*A*) 15-mil galvanized iron sheets, (*B*) 20-mil copper sheets

Sheet positive, electrode negative polarity

Using the calibration curve of Figure 5 for nickel-plated copper sheets, the experience curve for the entire sphere in terms of coulombs versus holes was obtained and is compared, in Figure 6, with the curve of strokes versus coulombs as obtained for the Empire State building. Considering the fact that the number of holes per stroke is not known in the case of the WSM sphere, the agreement seems quite good when it is remembered that in many cases there may be more than one hole per stroke. The Empire State building data come from measurements of oscillograms and are known to represent the complete stroke. Of course, the difference in height and location no doubt has also had some effect on the recorded results.

It is a matter of considerable interest to note the number of holes in the sphere below its equator compared with those above. A curve similar to that of Figure

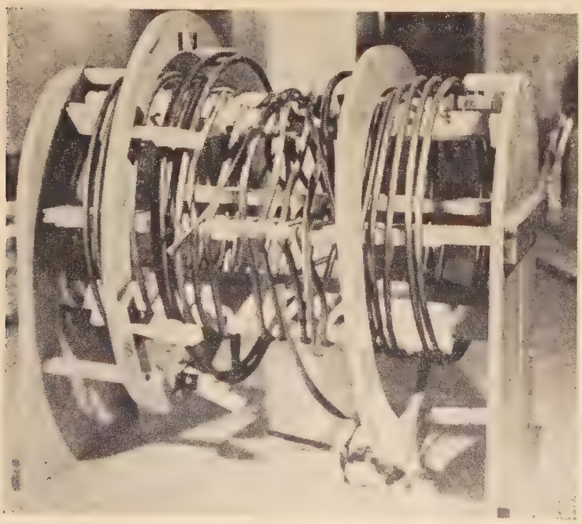


Figure 3 (left). Tuning coil of WSM radiator damaged by lightning stroke

Probable current crest 66,000 amperes—positive polarity. $^5/_8$-inch (outside diameter) tubing. 40-mil wall

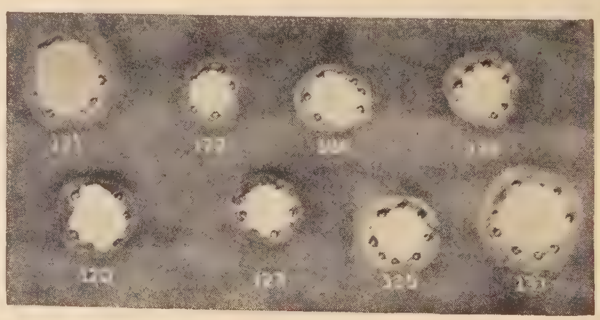


Figure 4 (right). Samples from d-c tests on 20-mil copper sheets to determine relation between current amplitude, length of application, and size of hole

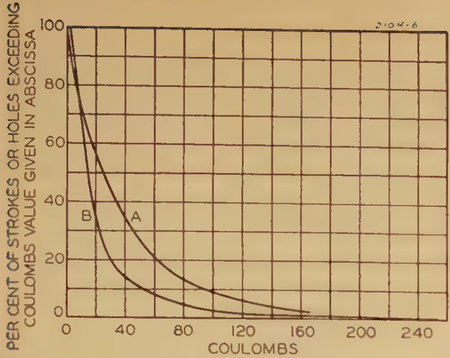


Figure 6. Expectancy curves for coulombs conducted in lightning strokes

A—Oscillographic measurement Empire State building
B—Hole calibration for WSM sphere

6 for the upper and lower halves shows ten coulombs at the 80 per cent point for the upper half and five coulombs for the lower, while for values below 50 per cent the curve for the upper and lower halves is about equal to and practically coincides with that shown in Figure 5 for the whole sphere. The total number of holes in the upper half is 89 and in the lower is 61. Within a three-inch belt above the equator, 44 holes were found, while in a similar zone below the equator, 41 holes were counted, with five holes in the seam around the equator. Thus, 60 per cent of the holes were located on 32 per cent of the total surface of the sphere. In view of the distance to the cloud, the field surrounding the sphere is quite uniform except for the effect of its support which would lead one to expect a shielded zone near the point of attachment, and this is just what was found. If the strokes represented by the holes were all initiated by the tower, one would expect streamers to be formed as the cloud field became more intense from all over the sphere's surface except close to the point

Figure 8. 265,000-ampere negative-polarity discharge through 10-mil plain copper sheet

Side away from arc. Sheet used as one electrode

of support. Chance conditions of ionization in the atmosphere probably have considerable effect in determining which of the streamers from the sphere will develop into the stroke.

It is quite likely also that the seam around the equator had a considerable effect in promoting the formation of streamers at the time of a stroke. Drops of rain water, particularly at the seam may have added to this effect. The top of the sphere was apparently shielded to some extent by a probable extension of the sphere support above the top of the

Figure 10. Exterior view of Amherst house struck by lightning

(*A*) Position of chimney. (*B*) Location of large hole. (*C*) Location of several small holes

sphere. The details of the attachment are not known.

Three hundred pits were found, many of these being accounted for as direct strokes having relatively high current magnitude, but having a low coulomb content (two coulombs or less). It does not appear to be possible to draw any definite conclusion concerning these pits in terms of current magnitude. It is possible, of course, that some of them are part of strokes which burned holes, and, if so, the effect on the calibration of holes in terms of strokes would be small on account of the small coulomb content.

In considering the interpretation to be put upon the results obtained by this sphere, it should be recorded that the number of separate strokes is not known, although Mr. Montgomery believes, based

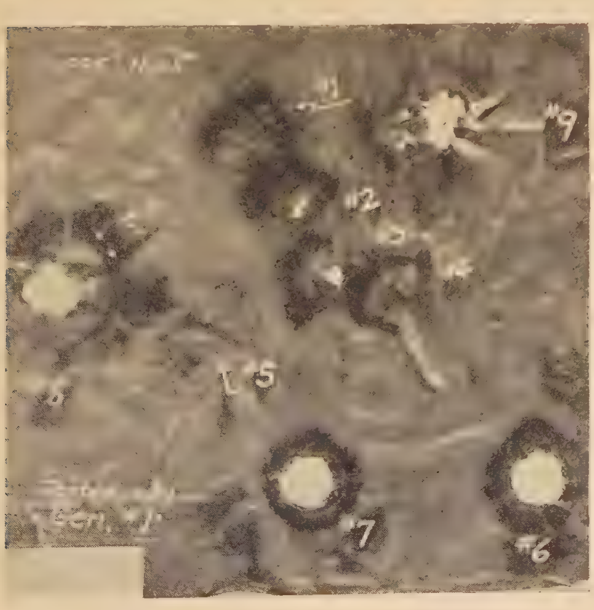


Figure 7 (left below). 40,000-ampere negative-polarity discharge through five-mil nickel-plated copper sheet

Side-facing arc. Small holes or burns numbers 1 to 5, sheet used as one electrode. Large holes numbers 6 to 9, sheet mounted between electrodes

Figure 9 (right). Holes burned by relatively low-current long-time laboratory discharges

Top, $^{3}/_{8}$-inch steel plate. Bottom, 0.015-inch galvanized tin. Side facing electrode

Figure 11. Damage to cement basement wall of A. T. Rouleau house, August 4, 1939, caused by lightning which came in across ground surface from nearby Joslyn house which was struck directly

Pelham Road near Amherst, Mass.

on his records, that 24 direct hits is a reasonable number to use. It is probable that this number is too low. With 150 holes, and assuming equal distribution of holes for all strokes, one arrives at seven holes per stroke which, from Figure 6, would indicate an average coulomb value of $7 \times 14 = 98$ coulombs, which is probably much too high, although the maximum hole indicates 240 coulombs. It must also be recognized that more than one stroke could contribute to the same hole as well as the same stroke making several holes.

At WSM, magnetic links had been installed on the antenna lead-in and guy wires at the time the sphere came down and the tuning coil of Figure 3 was damaged, which indicated a current of +66,000 amperes in the antenna lead-in and +6,400 amperes in two of the guy wires and a total of −73,000 amperes in six other guy wires. This record may cover more than one stroke, but it does indicate the highest currents recorded. It is interesting to point out that the most severe current peak measured with the cathode-ray oscillograph at the Empire State building[7] had a crest current of 58,000 amperes, contained 4.9 coulombs to half current value, and was positive.

Tests made in the laboratory on tubing similar to that of the tuning coil shown in Figure 3 indicate that the turns of the coil begin to pull together at a current crest of 75,000 amperes. The copper tubing itself begins to flatten at around 118,000 amperes. The difference between the effects of laboratory current amplitudes and the 66,000 amperes measured in the actual stroke is probably due to different wave shape, as well as the fact that two concentric coils were involved in the actual stroke. Furthermore, the heavy discharge current may have been preceded by a high coulomb–long duration discharge heating the copper tubing sufficiently to produce crushing at much lower crest currents. The laboratory current waves were oscillatory waves of 86 microseconds period with the initial peak of negative polarity. The tuning coil consisted of $^5/_8$-inch outside diameter tubing with 40-mil wall thickness.

Lightning Strokes to WGY

In 1938 a 20-mil chromium-plated copper cylinder was mounted on top of the then new WGY 635-foot radiator at Schenectady. The top of the cylinder was closed with a hemisphere of the same metal and thickness. When this was removed for examination in 1941, seven holes and several hundred small pits were found. The radiator was known to have been struck three times, the magnetic links showing −3,000, −7,800, and −30,000 amperes.

Of the seven holes, six were located in the 12-inch diameter hemisphere, mostly in the upper part, but none at the highest point of the sphere as installed. The cylindrical portion had a height of 14 inches, and one hole was found six inches from its lower edge. The pits were most numerous in the area above a line nine inches from the bottom of the cylinder. Twenty-nine of the pits had an appearance similar to that obtained from high-current laboratory discharges of one or two coulombs.

Concerning the Holes Burned

With reference to the appearance and size of holes some comment is desirable. While the WSM sphere showed some very small holes—as small as three square millimeters—such small holes could not be produced in the laboratory in copper sheets. The smallest hole obtained had an area of 28 square millimeters. However, in galvanized iron sheets, holes as small as 1.5 square millimeters could be produced.

This effect has been attributed to the close proximity ($^1/_4$ to $^1/_2$ inch) of electrode to the sheets under test. A tapered carbon electrode was used to avoid the effect of the metal of the electrode on the calibration. Although the electrode begins to glow at 60 coulombs, very little gas is given off. Brass electrodes were tried but consistent results were not obtained.

It is expected that the only difference to be obtained from a longer arc would be the possibility of forming smaller holes in copper sheets. Within the range investigated—spacing $^1/_4$ inch to $^1/_2$ inch—the character of the holes is not changed, indicating that the electrode effect is not appreciable. The close proximity of the electrode possibly has the effect of spreading the core of the arc wider than would be the case in a lightning stroke. However, the holes produced showed the same characteristics as those produced by natural lightning as evidenced on the WSM sphere.

The tests on metal sheets up to 100-

Figure 12. Lightning damage at W. H. Joslyn house, August 4, 1939. Pelham Road near Amherst, Mass.

Furrow in ground was dug by lightning as it left Joslyn house and headed toward neighboring Rouleau home

Figure 13. Destruction of pine tree by lightning discharge

Top of tree blown off by high current peak. Stump burned by continuing discharge

mil thickness indicate the following relations:

$$C=\frac{A}{25}\times t^{0.9} \quad (t=0 \text{ to } 35 \text{ mils})$$

$$C=\frac{A}{245}\times t^{1.54} \quad (t=35 \text{ to } 150 \text{ mils})$$

where

C = coulombs
A = area of hole in square millimeters
t = thickness of sheet in mils

Such equations are reasonably accurate for copper and galvanized iron and may be used to estimate the coulombs in a given lightning stroke. The use of the curves of Figure 5 for the individual metal is more accurate, but tests of this nature do not permit close calibration, the variation from the curves shown in Figure 5 being as much as 40 per cent for some individual points.

That the hole burning is a function of coulombs rather than i^2t has been verified by the fact that equal holes are burned when equal coulombs in the arc are produced with widely different current amplitudes. The test currents ranged from 50 amperes to 600 amperes, which is in the range of natural lightning continuing discharges. Tests at high currents with high coulomb values have not been made, but the calibration data given in Figure 5 should not be used for high currents. Bellaschi[2] similarly showed that burning of solid heavy electrodes is dependent upon coulombs.

In Figure 7 are shown holes burned in a five-mil, nickel-plated copper sheet with discharge currents of 40,000 amperes crest. The small holes and burns numbered 1–5 were produced with the sheet grounded. The larger holes 6–9 were obtained with the sheet midway between the line electrode and the ground electrode. The jagged character of the holes is apparent, especially in number 9 where the electrode distance was increased from $^5/_{16}$ to $2^3/_4$ inches. There is no evidence of the copper beads lining the hole as is observed on the sphere and the corresponding test sheets. As the current amplitude is increased, the explosive effect is increased as shown in Figure 8, which gives the effect of a 265,000 ampere 30-coulomb discharge to a grounded copper sheet of ten-mil thickness. The same impulse applied to a 25-mil copper sheet did not rupture it but did produce a large dent in its surface. On the other hand, a 25,000-ampere one- to two-coulomb discharge will form a bright bead on a 20-mil copper sheet but will not puncture the sheet. In the explosive type or high current discharge many factors have to be weighed besides current amplitude, while the charge in the long-duration low-current continuous discharge can be computed with fair accuracy.

The tests indicate a possible source of fire with thin metal-roofed buildings, if inflammable material is close to the metal or in such position that molten metal may drop on combustible materials. If thin metals are to be used for covering, then elevated air terminals should be used to keep the arc away from the building and a suitable path to ground provided to prevent damage at joints in the metal structure.

Tests on Steel Plate

In view of possible lightning strokes to metal containers of explosive gases of liquids, tests were made with the equipment shown in Figure 1 with the following result. A $^3/_8$-inch steel plate was tested with 430 coulombs in the arc. Results of such tests are shown in Figure 9. The plate could not be punctured but a crater-like hole was formed of $^3/_{16}$-inch depth and 180 square millimeters area. To puncture such a plate several thousand coulombs would be required.

Lightning Stroke to Home

Of the many lightning damage cases which the authors have investigated, that which occurred near Amherst, Mass., during the summer of 1939 is perhaps one of the most interesting. Lightning struck the home shown in Figure 10, resulting in the external damage shown. The upper portion of the chimney had been eliminated, and the chimney was cracked throughout its length. A hole having an area of 345 square millimeters was found in the 15-mil galvanized-iron shingle roof at a point about three feet from the chimney and in the direction of the damaged corner of the building shown in Figure 10. Several small holes were found in a shingle, which apparently had been located at the corner of the roof above the damaged corner. No evidence of fire could be found anywhere, although in the kitchen there was a considerable burn between a sink and a nearby pipe, which had a combined effective resistance to ground of 47 ohms. This was the only ground in this house. There was also considerable burning between the cover of an ice box and the zinc lining. Nearly all of the windows on the ground floor were blown outward, showing the high pressure developed within the house. Two persons were in the house, one upstairs to the right of the chimney, and the other in the kitchen to the left of the door shown in Figure 10. Neither was hurt.

There was no wiring of any sort in this home. In Figure 11 is shown another home, 155 feet away, which had a three-wire service with the usual appliances connected. In the cellar was a driven ground of 430 ohms. In addition, two grounds having a combined resistance of 215 ohms were driven at the service pole located 110 feet from the house of Figure 10, and 80 feet from the house of Figure 11. A portion of the path of the lightning discharge is shown in Figure 12. At the point illustrated the channel in the earth was about three feet in width and about 15 inches in depth. It continued with two-foot width and about the same depth for a distance of 74 feet where it split into two parts, one to continue 88 feet to the ground rod in the cellar of the house of Figure 11, and the other to travel 81 feet to the ground rods driven at the base of the service pole. The electric equipment in the wired house was in operating condition after the storm, although the cellar windows were blown out as the result of the pressure developed in the spark to the driven ground rod inside close to the cellar wall.

From magnetic effects observed, it was concluded that the current in the neutral wire was of the order of 30,000 amperes of a slow rate of rise and indicated a negative-polarity stroke to the unwired house of Figure 10. A calibration of the shingle burns indicates a coulomb value of 210 for the larger burn and 30 coulombs for the smaller burns.

In reconstructing what happened, it is probable that the stroke was negative, beginning with a current peak of the order of 200,000 amperes, followed by a continuing current having a coulomb value of 240. The physical damage seen in Figures 10 and 12 resulted from the high current peak. After the chimney exploded, apparently the path shifted to the burned points found on the roof. With the high coulomb value of the continuing current, one would have expected a fire to ensue, but no evidence of this could be found, indicating that the inflammability of the various paths was such as not to cause fire.

This case is given here, because some knowledge exists as to the magnitude of the lightning current which caused the damage observed. This again illustrates what may happen when good grounds are not available when lightning strikes.

The pine tree shown in Figure 13 is mute evidence of the same type of lightning discharge just described. This tree struck by lightning exploded a few feet

Abnormal Currents in Distribution Transformers Due to Lightning

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Synopsis: Abnormal currents in distribution transformers due to lightning are analyzed both theoretically and from field experiences. Under certain conditions of direct stroke transferred through the arrester to the neutral of the secondary winding, excessive currents may wreck the secondary windings by electromagnetic forces. Long-duration surges, or shorter repeated surges, saturate the transformer cores producing greatly increased surge currents in the primary windings which influence fuse failures. The saturation of the cores by unidirectional lightning surges also brings about increased power-frequency magnetizing currents influencing sectionalizing-fuse and circuit-breaker operations.

A GREAT deal of research has been done and many papers have been written dealing with the problems of overvoltages under lightning conditions. It has been assumed generally that, because of the relatively high inductance of transformer windings, very little lightning current builds up within them. There are, however, certain conditions under which the lightning current may enter the secondary windings of grounded-neutral distribution transformers and rise to such magnitudes as to develop electromagnetic forces sufficient to destroy that winding by mechanical stress. Also, for long-duration single or multiple lightning surges enough current builds up through the primary winding to saturate the core; and the consequent rate of increase of current in the winding becomes more rapid, and the current is finally limited only by the d-c resistance of the primary winding. These latter currents are generally insufficient to damage the winding, but they are important in the determination of fuse failures[1] and sectionalizing operation under lightning conditions.

Another source of abnormal transformer currents is that of large magnetizing inrush currents brought about by surge conditions, which will be primarily important in the operation of fuses or breakers. These various phenomena of abnormal transformer currents are discussed further in this paper, with special reference to distribution transformers on rural lines.

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Direct-Stroke Lightning Currents in the Secondary Windings

Under certain circumstances of direct lightning strokes to the primary line, very large currents may go through the secondary winding when the lightning arrester functions to protect the primary. Occasionally the effect of such currents is completely to disrupt the windings, as shown in Figure 1. There is no present protection scheme in use which protects against such conditions. The transformer was connected as shown in Figure 2. The lightning arrester effectively by-passed to the ground lead the resulting current from a direct stroke to the line, and the primary winding was perfectly protected, as determined by subsequent test. The voltage of the secondary wires rose, together with that of the transformer case, to the value of the surge-impedance drop of the grounding wire impedance and ground resistance, and another flashover to ground occurred, this time on the customer's premises. Current in the two wires on each side of the grounded neutral to the customer ground fault went through the secondary winding. Since the current went from the transformer case at raised potential into the neutral connection of the transformer coil, and from there into the winding halves in opposite directions, the magnetic fields of the two halves of the secondary winding canceled out to a high degree, because of the close coupling. The leakage-reactance drop and also the resistance drop were not sufficient to flash over the bushing or the normal shunt safety gaps in use. Thus the current continued to go into the secondary coil sections in opposing directions; the extreme resulting mechanical forces due to electromagnetic reaction distended the secondary winding, as shown at X in Figure 1, and actually cut several turns in two by jamming them against the core laminations.

Such conditions to a greater or lesser extent, must occur in almost every case of a direct stroke to line near a transformer installation. The paths for the lightning current through the transformer secondaries are indicated by arrows in Figure 2. The relative amount of current going into the windings varies with the relative ground resistances; in this case it was probably of the order of 20,000 amperes

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above the ground. Examination of the upper portion of the tree and the debris scattered over a considerable area showed no signs of burning; yet the stump was on fire when the owner of the land ran to the point where he saw the lightning strike. Evidently this stroke also began with a high current peak blowing the tree apart. The stroke persisted as a continuing current to the stump of the tree, which was still grounded, setting it afire. This was going on while the tree and the debris were in the air.

The data which have been collected thus far seem to indicate that the use of thin metal surfaces makes a fairly satisfactory means for collecting data with respect to the number of coulombs contained in natural lightning strokes, excluding the effects of the high current peaks which may be present.

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in each transformer section, but in most cases the current is apparently low enough to be harmless.

Insufficient field data are available on the frequency of such failures, and therefore it is uncertain whether protection against such faults is economically feasible. Also, it is beyond the scope of this paper to consider protective means beyond mentioning a few obvious measures such as:

1. Reduction of arrester ground resistance to a value considerably less than the customer's ground, although the customer's ground must also be kept low for his safety.

2. Application of very low breakdown-value protectors across the secondary bushings.

3. Placing of small reactors in the transformer secondary leads inside the tank, which will cause the bushing gaps to flash over and protect the secondary winding.

4. Placing the two secondary leads inside a waterproof metallic cable, the metallic covering of the cable serving as the neutral lead grounded to the case, and also serving as a supplement to the arrester ground when the cable is run underground to the customer.

Primary Winding Currents Under Long-Duration Surge Conditions

Very little surge current is built up in the primary winding until the transformer core reaches saturation, when the effective primary-winding inductance drops sharply; short-duration surges do not produce saturation. Long-duration or repeated surges equivalent to long surges can build up considerable currents, often sufficient to blow the usual primary protective fuse. The determining factors in this saturation phenomenon can be more readily analyzed by taking a specific field example of a 7,200-volt rural distribution system feeding a network of three-kva transformers, shown in Figure 3.

There are two possible conditions under which long surge waves may be present on the system. The arresters on the system may be functioning and, if they are of the conventional type, they will limit the peak voltage to about 55 kv, but this will drop at a slower rate than the incident wave; thus, in effect, producing a lengthened surge. On the other hand, especially in the case of repeated discharges, long surges may be present on the system without any arresters functioning on some of the later of the repeated discharges which may have dropped to below the breakdown point of the arrester. In the latter case the duration of the surge is influenced by the discharge path both from cloud to ground and within the cloud itself. Recent investigations[2,3] show such waves may be from 1,000 to 20,000 microseconds duration. A border-line case which would fit the above conditions of the arresters functioning or not functioning could be represented on the particular 7,200-volt line by a surge expressed as

$$e=E_s\epsilon^{-\alpha t}=55{,}000\epsilon^{-0.0003t}$$

This expression corresponds to a wave having a crest value of 55 kv and dropping exponentially to half value in about 2,500 microseconds. It will be noted that the second term of the more general expression for a surge, $e=kE(\epsilon^{-\alpha t}-\epsilon^{-\beta t})$, which is omitted, represents the rate of rise of the front of the wave. The duration of the "front" of the wave is so short in relation to the very long duration of the "tail" of the wave that it plays little importance in the determination of the final current through the winding; and therefore, the corresponding term is left off to simplify the mathematical presentation.

This surge wave is so long relative to the time of travel along the entire transmission line that the same potential can be considered applied at all the transformer installations, and the surge impedance of the line can be considered a negligible factor. This becomes clear when it is considered that the length of the wave of 2,500 microseconds corresponds to a time such a wave could travel 475 miles, or would travel back and forth in successive reflections on an average 50-mile rural system about ten times while dropping to half value. Hence the conditions can be considered essentially the same at each of the distribution transformers and may be schematically represented as shown in Figure 4.

The curves C and F, Figure 4, show the resulting current rise in the primary winding with the surge occurring at different points C' and F' on the normal 60-cycle wave, shown in Figure 5. The current begins to be appreciable only as the transformer core saturates. The hysteresis loop A-B-C-D-E-F-G in Figure 5 gives the normal variation of B-H as the transformer core changes its values of B to produce the equal but opposite sinusoidal electromotive force, $iR+N(d\Phi/dt)\cdot 10^{-8}$,

Figure 1. Showing the effect at X of electromagnetic stresses caused by lightning currents entering the secondary sections of a distribution-type transformer at the neutral-wire connection

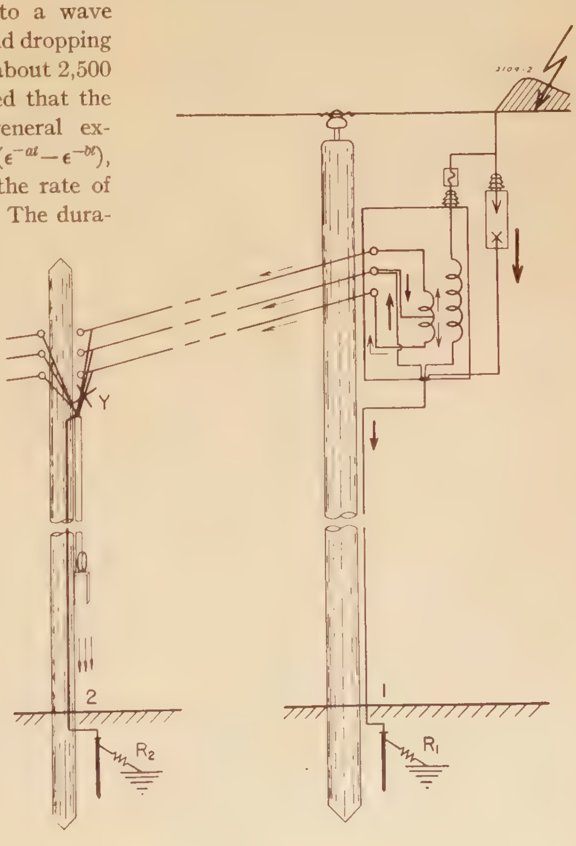


Figure 2. Schematic diagram of a typical rural-distribution-transformer connection with arrows indicating how the lightning current may penetrate the transformer secondaries

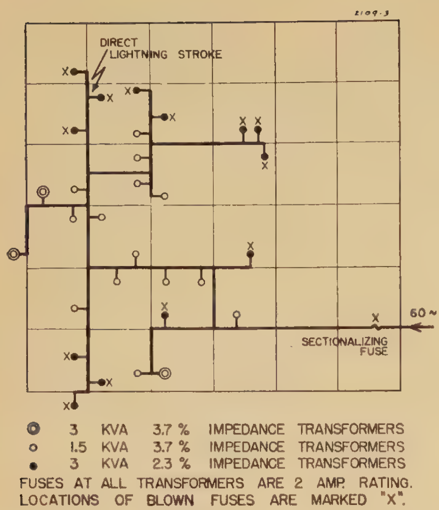
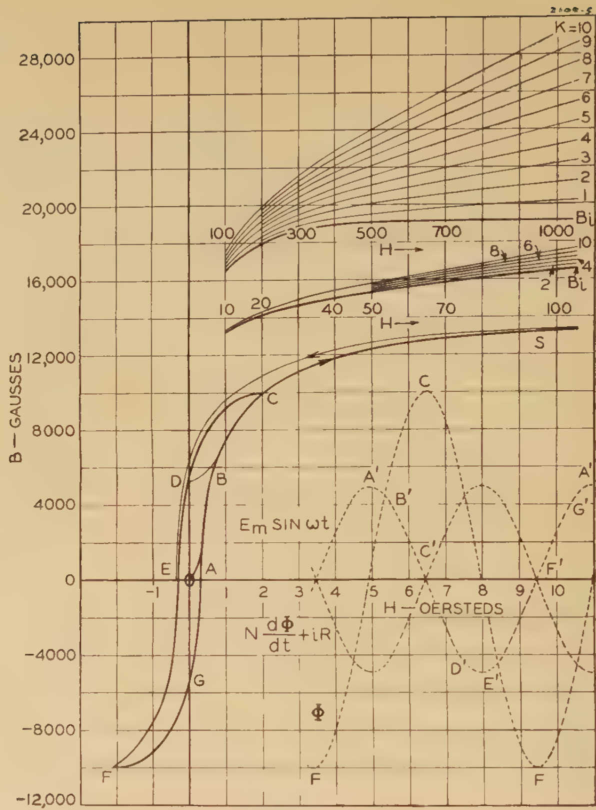


Figure 3. Typical 7,200-volt rural single-phase section in which fuse failures were caused by lightning current penetrating the primary windings of the distribution transformers

Figure 5. Magnetization curve for the core of the typical distribution transformer

The dashed lines indicate relative flux-voltage relations under normal operation



which balances the applied electromotive force, $E_m \sin \omega t$. The B and electromotive force relationships are indicated in dashed lines on Figure 5, from which it is clear that a surge impressed at C' on the 60-cycle voltage wave conforms to C or maximum flux value, on the hysteresis loop, will cause a maximum flux increase along S; and, therefore, will result in the shortest time to core saturation. Similarly a surge at point F' on the 60-cycle wave would result in the longest time to saturation. The calculation of the growth of current must take into account these various states of magnetization of the core; and because the B-H curves cannot be expressed mathematically by a simple equation, the fundamental equations must be solved graphically or in a step-by-step manner, as given in the appendix. From these calculations and the resulting

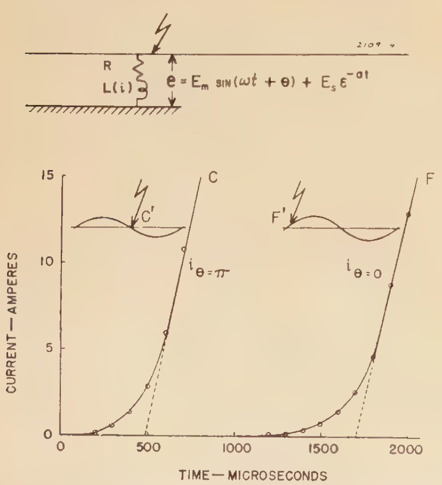


Figure 4. Initial current rise in a distribution-transformer primary due to lightning surges incident at different points on the normal power-frequency cycle

curves of Figure 4, it is clear that the time denoted by t_o elapsing before the core saturates and appreciable currents can develop varies with the point of surge incidence on the 60-cycle wave and lies in the range $t_o = 1{,}100 \pm 600$ microseconds.

The above range of t_o was determined for one specific 7,200-volt transformer of three-kilovolt-ampere rating. It follows, however, from the calculations (see appendix) that the time t_o is independent of the transformer kilovolt-ampere rating and of the voltage rating of the transformer, so long as the ratio of its voltage to the surge voltage remains the same. It also follows, from the calculations and the curves of Figure 5, that for the larger values of current as the core saturates, the relation of B to H increments or $\Delta B/\Delta H$ becomes a constant, and the problem may be solved as with constant parameters. The relation for the current due to a surge potential $e = E_s\epsilon^{-at}$, may be written as

$$i = \frac{E_s}{R-aL}[\epsilon^{-at} - \epsilon^{R/L(t-t_o)}]$$

where L is the inductance of the circuit with the core saturated, t_o is $1{,}100 \pm 600$ microseconds, depending on the point of surge incidence on the power frequency cycle; the values of i up to $t = t_o$ being relatively small and considered zero.

The derived simplified equation lends itself readily to the examination of possible currents flowing into various transformers having different impedance characteristics. Thus, Figure 6 shows possible currents due to the surge alone flowing into two transformers of different impedances but of the same kilovolt-ampere and kilovolt ratings. These currents are of sufficient magnitude to cause the protective fuses in the transformer-primary-circuit leads to fail, as was proven by instances of fuse failures on *unenergized* lines under lightning conditions. Conclusive evidence that the relative currents are as calculated is given by the field case summarized in Figure 3, where every fuse failed at a 2.3 per cent impedance transformer but where none failed at any of the higher impedance transformers. The greater current associated with the lower impedance transformer, as shown

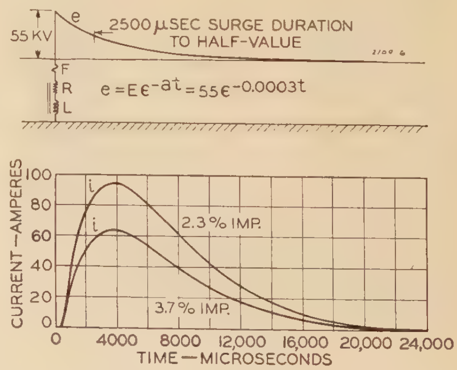


Figure 6. Incident surge wave and resulting current in the primary windings of three-kilovolt-ampere, 7,200-volt distribution transformers having per cent impedance values as marked on the curves

in Figure 6, accounts for the selective action noted. The solution of the fuse-failure problem is definitely not, however, the use of high-impedance transformers. In this particular case all of the fuses were of the same rating; and none failed on the high-impedance transformers, because these transformers were overfused relative to their impedance and, therefore, were actually unprotected against normal overloads.

Conclusion

Direct lightning strokes may cause very large currents to penetrate the secondary windings of distribution transformers with destructive effects, even though the primaries are perfectly protected. It is important, therefore, to determine the actual extent and frequency of occurrence of this type of failure in practice.

Long-duration surges cause relatively large currents to build up also in the primary windings of distribution transformers, as analyzed in this paper. Field experience with fuse failures and laboratory research by the writers with simulated surge conditions, have checked the validity of the analytical approach. Oscillographic records obtained in this research agree closely with Figure 6. Conclusions on the effect of variations of incidence of a surge on the 60-cycle wave follow readily and accurately from the detailed analysis in the appendix and elsewhere in this paper. Published experiments by Bergvall and Beck[4] with direct currents equivalent to long-duration surges applied to transformers are also in reasonable agreement.

The calculations of Table I in the appendix, extended over a number of cycles, show that even after the surge voltage has completely disappeared a high magnetizing current transient results, due to the high degree of saturation in the core. Reference to equation 5 in the appendix shows that the flux increment, due to a small surge incident on the transformer 60-cycle wave at a point corresponding to low core flux value, may be written

$$\sum \Delta B = \sum \frac{\omega B_m}{E_m} E_s \epsilon^{-at} \Delta t$$

since iR would then be negligible during the surge-incidence time. The total flux increment due to a surge $E_s\epsilon^{-at}$ would be

$$\int_0^\infty \frac{\omega B_m}{E_m} E_s \epsilon^{-at} dt = \frac{\omega B_m E_s}{a E_m}$$

It follows, therefore, that every surge, no matter how small, would cause a certain shift in flux and would produce a magnetizing transient. These transients, although usually more severe, are of the same character as the magnetizing inrush currents experienced when energizing a transformer at certain points on a 60-cycle wave. This fact is of importance in examining possible faulty operation of fuses and breakers. An interesting proof that such "induced" magnetizing inrush currents may cause faulty fuse and breaker operation is given by cases of recent outages during periods of severe sun-spot activity which resulted in long-duration relatively constant voltages across the transformers, which although quite different in magnitude were comparable in effect to long-duration repeated lightning surges.

Although complete protection methods have been developed, further researches and field studies are necessary to determine the frequency of occurrence of these problems in actual practice.

Table I. Step-by-Step Solution of Current Due to a Lightning Surge, $55,000\epsilon^{-0.0003t}$ Superposed at the 180-Degree Point of the Normal Power-Frequency Cycle

2109-7

$$\Delta B = -10000\ \Delta\cos(0.000377t+\pi) - 67\,700\ \Delta\epsilon^{-0.0003t} - 0.1\,i\,\Delta t$$

1	2	3	4	5	6	7	8	9	10	11	12	13	14
t	Δt	ωt	$\cos\omega t$	$10^4\Delta\cos\omega t$	at	ϵ^{-at}	$\Delta\epsilon^{-at}$	$-67700\Delta\epsilon^{-at}$	i	$-0.1\,i\,\Delta t$	ΔB	$\Sigma\Delta B$	$i=0.01H$
t	Δt	.000377 × (1)	cos (2)		.0003 (1)	$\epsilon^{-(6)}$		−67700 (8)			(5)+(9)+(11)		
0		0	1.0000		0	1.0000						10 000	0.02
100	100	0.0377	0.9993	−7	0.03	0.9704	−0.0296	2010	0.03	−0.3	2000	12 000	0.045
200	100	0.0754	0.9971	−22	0.06	0.9418	−0.0286	1940	0.10	−1.0	1920	13 920	0.16
300	100	0.1131	0.9936	−35	0.09	0.9139	−0.0279	1890	0.3	−3	1850	15 770	0.52
400	100	0.1508	0.9886	−50	0.12	0.8869	−0.0270	1830	0.93	−9	1770	17 540	1.35
500	100	0.1885	0.9823	−63	0.15	0.8607	−0.0262	1770	2.0	−20	1690	19 230*	2.70
600	100	0.2262	0.9745	−78	0.18	0.8353	−0.0254	1720	4.3	−43	1600	20 830	5.90
700	100	0.2639	0.9654	−91	0.21	0.8106	−0.0247	1670	8.3	−83	1500	22 330	10.80**
800	100	0.3016	0.9549	−105	0.24	0.7867	−0.0239	1620	13.1	−131	1380	23 710	15.40
900	100	0.3393	0.9430	−119	0.27	0.7634	−0.0233	1580	17.5	−175	1290	25 000	19.70
1000	100	0.3770	0.9297	−133	0.30	0.7408	−0.0226	1530	21.7	−350	1180	26 180	23.63
2000	1000	0.7540	0.7280	−2017	0.60	0.5488	−0.1920	13000	35.9	−3590	7390	33570	48.26
3000	1000	1.131	0.4257	−3023	0.90	0.4066	−0.1422	9620	50.8	−5080	1520	35090	53.33
4000	1000	1.508	0.0628	−3629	1.20	0.3012	−0.1054	7140	50.7	−5070	−1560	33530	48.13
5000	1000	1.885	−0.3090	−3718	1.50	0.2231	−0.0781	5280	43.5	−4350	−2790	30740	38.83

* Complete saturation of iron in core, beyond which $\Delta B = K\Delta H$.

** Values beyond B-H curve obtained from $\Delta i = 0.01\,\Delta H = 0.01\Delta B/K$.

Appendix. Solution for Current in Transformer Due to Lightning Surge Superposed on the Normal Power Cycle

The fundamental relationship for the transformer with a total voltage e across it, composed of the normal operating voltage $E_m \sin \omega t$ and a superposed surge voltage $E_s\epsilon^{-at}$ may be written as

$$\begin{aligned} e &= E_m \sin(\omega t+\theta) + E_s\epsilon^{-at} \\ &= N(d\phi/dt)\times 10^{-8} + iR \\ &= A(dB/dt) + iR \end{aligned} \quad (1)$$

where ϕ represents the total value of the flux linking the primary winding, including the air space around the core, that is, the leakage inductance $L(di/dt)$ drop. This leakage inductance is introduced in the calculation by choosing the graph corresponding to the proper value of K in Figure 5, the value of K being equal to the ratio of total leakage flux (excluding the contribution of the core) to the leakage flux which would be obtained in the space occupied by the core. External inductance of transmission lines, and so forth, can also be included in determining the value of K, as suggested by Steinmetz[5] in solving the somewhat similar problem of determining magnetizing inrush current. The value of the constant A can be determined from the physical characteristics of the winding or from the known maximum flux density under normal operating voltage conditions. Neglecting the iR term for normal magnetizing currents, and for normal steady-state conditions

$$E_m \sin \omega t = A(dB/dt)$$

from which

$$dB = (E_m/A) \sin \omega t dt \quad (2)$$

$$B = (E_m/A)\int \sin \omega t dt = (-E_m/A) \cos \omega t$$
$$B_m = E_m/\omega A$$

and hence,

$$A = E_m/\omega B_m \quad (3)$$

Thus, substituting this value of A in equation 1 for surge conditions

$$\begin{aligned} e &= E_m \sin(\omega t+\theta) + E_s\epsilon^{-at} \\ &= (E_m/\omega B_m)(dB/dt) + iR \end{aligned} \quad (4)$$

For a solution of equation 4, dB/dt may be written in finite increment form $\Delta B/\Delta t$, giving

$$\Delta B = (\omega B_m/E_m)(e - \bar{i}R)\Delta t \quad (5)$$

The values of the total potential e composed of $E_m \sin(\omega t+\theta)$ and $E_s\epsilon^{-at}$ in equation 5 must be the average values over the interval Δt. To avoid the approximation of finding the average, $E_m \sin(\omega t+\theta)dt$ may be written as $-d[(E_m/\omega)\cos(\omega t+\theta)]$, and $E_s\epsilon^{-at}dt$ as $-d\left[\frac{E_s\epsilon^{-at}}{a}\right]$ so that, in substitut-

ing increments for the differentials, equation 5 may be written as

$$\Delta B = -B_m \Delta \cos(\omega t + \theta) - \frac{\omega E_s B_m}{a E_m} \Delta \epsilon^{-at} - \frac{\bar{i} R \omega B_m \Delta t}{E_m} \quad (6)$$

The values of i remain to be averaged over the interval Δt, so that, where variations of i are rapid, it may be necessary to use shorter intervals of t. The values of ΔB are added to the preceding values to give the total B, and the corresponding values of i are determined from the B-H curve of Figure 5 in the step-by-step manner indicated in Table I. The relation of i to H depends upon the number of turns in the winding, N, and the mean length of the magnetic path, l;

$$H = \frac{0.4\pi N i}{l} \text{ or } i = \frac{Hl}{0.4\pi N} \quad (7)$$

For a typical three-kilovolt-ampere, 7,200-volt distribution transformer it was found that for $E_s = 55$ kv, as shown for the surge $e = E_s \epsilon^{-at}$, and for the actual constants as determined from the transformer of $B_m = 10{,}000$ and $E_m = 7{,}200\sqrt{2}$ or 10,200 volts, and $R = 280$ ohms, equation 6 for ΔB could be expressed with convenient coefficients for step-by-step calculations, to within two to three per cent probable error, as

$$\Delta B = -10{,}000 \Delta \cos(0.000377t + \theta) - 67{,}700 \Delta \epsilon^{-0.0003t} - 0.1\bar{i} \Delta t \quad (8)$$

with t and Δt expressed in microseconds, and where, for the particular transformer $l = 32$ inches and $N = 6{,}240$ turns, the values of i are given by

$$i \approx 0.01 H \quad (9)$$

with H corresponding to the various values of B determined for the proper value of K (in this case $K = 3$) from Figure 5.

The initial value of B in Table I is taken as that corresponding to the point on the normal 60-cycle voltage wave at which the surge is incident. The particular value taken in the given tabulation is that value nearest saturation, or with an initial value of $B = B_m = 10{,}000$, corresponding to the incidence of the surge at the zero point C' on the 60-cycle wave, or where $\theta = \pi$ in equation 6. It is clear from the tabulation and from the corresponding plotted curve in Figure 4, that the values of current are relatively small until the time when the core is very near the saturation point, or until $t_o = 500$ microseconds in this case. Similar calculations for a point of incidence of the surge corresponding to $\theta = 0$ in equation 6 and to F' in Figure 5 gives, for the equivalent time to saturation t_o, the value of 1,700 microseconds. Thus the time elapsing before the core saturates and appreciable currents can develop varies with the point of surge incidence on the normal voltage wave, and for the given ratio of

$$(E_s/E_m) = \frac{55{,}000}{10{,}200} \text{ lies in the range}$$

$$t_o = 1{,}100 \pm 600 \text{ microseconds} \quad (10)$$

The above value of t_o was determined for one specific 7,200-volt transformer of three-kva rating. It is of interest to refer back to equation 6 from which it will be noted that while i is still small, that is, before $t = t_o$, the values of ΔB due to the surge voltage are proportional to (E_s/E_m). This proportionality means that for the same ratio of surge voltage to line voltage the values of $t_o = 1{,}100 \pm 600$ microseconds will hold closely, irrespective of the transformer rating, and will vary only in response to (E_s/E_m). Thus, since there is a fairly well-established ratio of allowable surge voltage to 60-cycle voltage on distribution lines the values for t_o are valid for all distribution transformers, irrespective of kilovolt and kilovolt-ampere ratings.

Another interesting development which follows upon examination of Table I and Figure 5 is that, as the core saturates the relation $(\Delta B/\Delta H)$ becomes a constant and the problem may be solved approximately with equivalent parameters. If one neglects the much smaller component of the 60-cycle voltage effective under the surge conditions of saturation, the current due to a surge potential $e = E_s^{-at}$ may then be found from,

$$i = \frac{E_s}{R - aL}\left[\epsilon^{-at} - \epsilon^{-(R/L)(t - t_o)}\right] \quad (11)$$

where L is the inductance of the circuit with the core saturated, t_o is $1{,}100 \pm 600$ microseconds dependent on the point of surge incidence on the 60-cycle wave, and the values of i up to $t = t_o$, being small, may be neglected.

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Current Ratings of Electronic Devices for Intermittent Service

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Synopsis: Electronic devices are used extensively for switching, relaying, welding, and similar service resulting in intermittent loads. In the paper methods for assigning standard ratings suitable for such intermittent loads are investigated, both for single-anode tubes and tube circuit combinations. Vapor-filled tubes with oxide-coated filaments are treated somewhat in detail in order to develop the basic considerations. Other types of tubes, such as the pool-type and cold-cathode, are only briefly discussed. A method of rating which seems generally applicable is proposed, with the suggestion that its suitability for the great variety of conditions encountered be further investigated.

THE assignment of ratings especially suited for intermittent service is desirable, if it results in more economic use of devices than the present practice of assigning continuous ratings. The subject is of particular importance in connection with tubes, because they are used so extensively for intermittent operations such as switching, relaying, signaling, spot-welding, and so forth. It is intended in this paper to examine the possibilities of assigning intermittent service ratings to electronic devices, giving due consideration to the methods proposed in the recent AIEE report No. 1-A on "General Principles for Rating of Electrical Apparatus for Short-Time, Intermittent, or Varying Duty," by standards co-ordinating committee 4.[1]

In any analysis of the rating of electronic devices the following must be kept in mind:

1. The flow of current through each anode, and consequently through a single-anode tube, is intermittent in nearly all applications. This is shown in Figure 1, case A_1, which illustrates the current flowing with a single-anode rectifier. The same condition applies with a single-phase full-wave rectifier for each anode. Case C_1 shows the current in each single-anode tube for three-phase rectification; similarly E_1 applies to six-phase arrangements. In some applications the time of current-flow is even less, as in the ignitor circuit tubes of ignitrons, illustrated by case G_1. If the application is such that the current-flow periods repeat continuously, it is customary to consider it continuous service, which for each anode means continuously repeating pulsations of the current. Regardless of this continuous feature, it must be realized that dependent upon the duration of each pulsation and the amount of idle time between pulsations, different current rating values may have to be assigned for the different cases of Figure 1 and similar variations, in order to bring about economical use of the electronic devices.

2. In all electronic arrangements consisting of two or more single-anode tubes, careful distinction must be made between the rating of individual tubes and that of the complete tube combination. Cases B_1, D_1, and F_1 show the output of two-, three-, and six-tube arrangements corresponding to the single-anode currents of cases A_1, C_1, and E_1, respectively.

3. The output of the tube combination may be continuous as discussed so far, or intermittent as illustrated for equal time on and off in B_2, D_2, and F_2 of Figure 1. The currents in the individual single-anode tubes for these cases are shown in A_2, C_2, and E_2 respectively. The current here is intermittently pulsating. G_2 shows an intermittent load for short-time individual pulsation as might be found in the individual ignitor circuit tubes used in seam-welding. Ratings for such intermittent tube loads may be different from those of the previously described continuously pulsating loads.

Rating of Single-Anode Tubes

Depending upon the type of tube and type of load, the permissible rating of a single-anode tube may be limited by any one of the following factors:

(*a*). *Heating of the Tube as a Whole.* With the usual assumption of constant voltage-drop in gas-filled tubes, it is customary to consider the average current as the principal factor in this respect. Investigations by Knowles and McNall[2] indicate, however, that with short-time pulsations in a hot-cathode tube the assumption of constant voltage-drop does not hold true, and this may have to be considered under some conditions. (In the case of rectification of frequencies of hundreds or thousands of cycles, there is considerable additional anode heating due to positive ion bombardment of the anode during the inverse part of the cycle, a fact which, of course, must receive due consideration.)

(*b*). *Heating at the Lead-in Conductors and Seals.* This is influenced largely by the rms current, and if it is too high difficulties may develop. (Unfortunately the heating of the lead-in conductors and of the tube as a whole are interdependent, making a clear-cut distinction between the limits imposed by the average current and the effective current difficult.)

(*c*). *Values of the Individual Current Pulsations and Their Duration and Frequency.* These may be important factors in limiting tube ratings for a number of reasons dependent on the physical phenomena entering into the operation of various types of tubes. (The sparking tendency of oxide-coated filament tubes and the back-fire phenomena in pool-type tubes are examples of this.)

(*d*). *Requirements for Satisfactory Life of the Tubes.* Even though the limitations imposed by items a, b, and c have been taken into account to assure satisfactory operation, rating values may have to be made the subject of further adjustments to assure satisfactory life of the tube. (The life of oxide-coated filaments or the life of the covering of cold cathodes are examples of this. The electron emission in a hot-cathode tube decreases during life, and in many cases the end of life is determined by lack of cathode emission. Therefore, tests or ratings involving peak-current-carrying capacity, as well as any other current rating, of the tube must take this into account.)

Separate consideration will have to be given to each type of tube for various service conditions before any plan toward a reasonably uniform method of rating can be devised. As a contribution toward a satisfactory system of rating a number of typical cases will be considered in the following.

Vapor-filled tubes with oxide-coated filaments will be considered first. Rating data for those tubes usually give the continuous average current rating and a limiting peak or crest value for the current. The latter ranges in some tubes from four to six times the continuous average current value, or conversely, the continuous average is 25 to 16.6 per cent of the specified peak value. In Figure 1 the rms and average current values are indicated in per cent of the crest values for each of the cases shown. It will be noted that in all cases of the single-anode tube currents except A_1 and C_1 the average current is less than 16.6 per cent. This in turn means that with the data indicated above, which are those usually given, the average current is the limiting factor in cases A_1 and C_1, while in all other cases the specified peak current limits the permissible load, regardless of the duration and frequency of the current impulses. A question therefore arises as to whether better utilization of these devices might not be brought about by some other method of specifying ratings more suitable for loads with short-time pulsations. Knowles and McNall have made an investigation on the sparking point of

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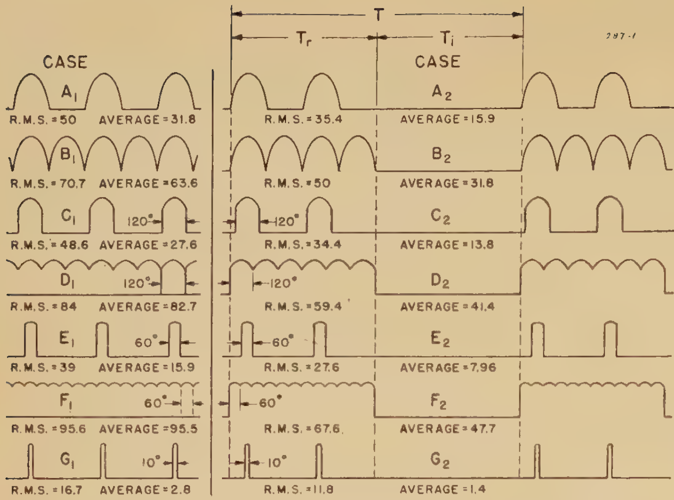


Figure 1. Typical load current for tubes and tube combinations

Cases A_1, A_2, C_1, C_2, E_1, E_2, G_1, G_2—Single anode currents

B_1, B_2, D_1, D_2, F_1, F_2—Currents for multi-anode arrangements

A_1 to F_1—Continuous loads

A_2 to F_2—Intermittent loads

Rms and average current values in per cent of crest values are indicated in each case

the filament as depending upon current and time. Some of their results are rearranged in Figure 2. It will be noted that with the minimum filament voltage of 2.25 volts, peak currents of 2.75 amperes for long durations of the pulsation are indicated. The curve also shows that the permissible peak loads can be increased by 2 per cent, 11 per cent, 32 per cent, and 80 per cent, for 180, 120, 60, and 10 degrees of the 60-cycle wave respectively; especially in the last two cases the gain is appreciable and seems to make it worth-while to standardize on several ratings suitable for service with various short-time pulsations.

The underlying physical phenomena make it necessary to specify for each of such ratings:

1. The wave shape of the impulse.
2. The crest value of the impulse.
3. The duration of the individual impulse.
4. The frequency at which the impulses can be safely repeated.

1. *Wave shape.* The tests by Knowles and McNall were made with a rectangular impulse wave. In actual practice a variety of waves is likely to be found, some of which are shown in Figure 3. Although all of these waves have the same crest value and the same total time of current-flow, it is obvious that the effect of wave A, for instance, upon the sparking tendency and other characteristics of the tube is likely to be considerably different from that of wave F. Since it is obviously impracticable to establish a multitude of standards for all these wave shapes, a few typical ones will have to be selected as a basis for standardization; the choice will be determined largely by the wave shapes encountered in practical applications. A near rectangular or a sinusoidal wave, or both, may at times be selected for this purpose. In the application of tubes waves approaching a rectangular wave shape (see cases C to G of Figure 1) are at least as frequent as sine waves (A and B). The ease with which tests, especially life tests, can be made with a given type of wave should probably also have an important influence upon the final choice. Multiphase arrangements of single-anode tubes on 60-cycle circuits with at least six phases (six tubes) give a wave shape approximating a rectangular wave (Figure 1, case E_1), and thus seem to be very convenient for test purposes. Therefore, standardization with such a wave shape of the impulse as a basis seems advisable in addition to standardization with a sinusoidal wave shape. Tests for standards with shorter impulses can be made either by increasing the number of phases (tubes) or by using basic frequencies above 60 cycles, depending upon the case. (In case of rectangular waves the steepness of the wave front may have to be specified, as destructive cathode bombardment may result if full anode current is drawn in time less than ionizing time of tube.)

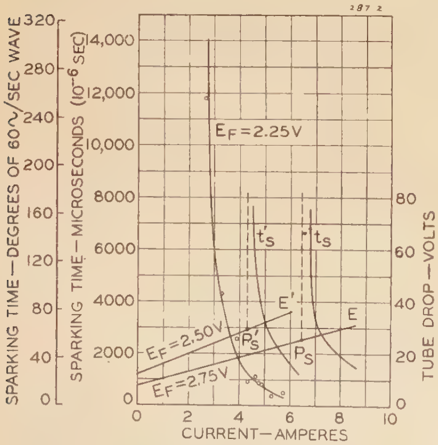


Figure 2. Current values dependent upon time above which cathode sparking may occur in a vapor-filled oxide-coated-filament tube

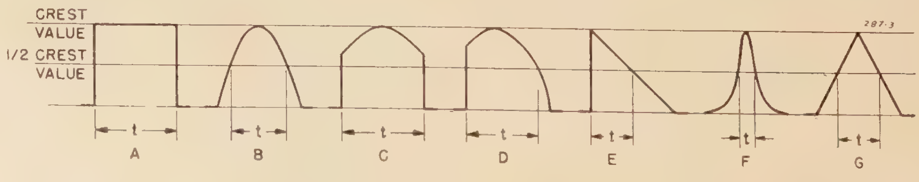
2. *The crest value of the impulse* has the advantage of simplicity over the use of rms or average values, since it obviates in many instances the necessity for calculating these latter values

3. *The time of the individual impulse* may be expressed in units of time such as milliseconds or microseconds; this practice has the advantage of being most generally applicable to all conditions found in actual practice. However, in some applications it may be convenient to express the time in degrees of some standard frequency, such as 60 cycles. (If a near-rectangular wave with a peak value and time specified is used as a basis for standardization, a question naturally arises as to how a tube so rated can be applied to pulsations having different wave shapes, such as shown in Figure 3. A practical and safe method can be based on the assumption that current values below a certain percentage (possibly below 50 per cent) of the peak value will have but negligible effect upon either the sparking tendency or the life of the tube. If this is true, the time t during which the current exceeds such percentage can be safely considered the effective time of the impulse, as illustrated in Figure 3. For example, an application with a wave shape as in case F would use a tube standardized on a much shorter rectangular impulse duration than would be used in D, although the total time of the current flow is the same in both cases.)

4. *The frequency at which the impulses can be safely repeated,* either continuously or for a limited period, must be given with the rating data. The tests on sparking tendency by Knowles and McNall were carried on with single impulses. If several such impulses were to follow each other at very close intervals, their effect might approach that of one longer interval, and sparking might result. If on the other hand the impulses are repeated at not too frequent intervals, the only result is likely to be a slight increase of the temperature of the filament, which in general will have a favorable effect on the operation at the lower filament voltages and no particularly harmful effect at the higher filament voltages. If the impulses are repeated too frequently for extended periods, limitations may also be reached, because of exceeding the safe average or rms currents of the tube, or because the life of the tube is decreased below satisfactory values. Various other limitations may have to be considered in this connection. The maximum frequency of impulses at which any of the limitations are reached can be readily determined by test, so that usually it is not difficult to establish a maximum frequency that is safe. In many arrangements connected to commercial circuits with a definite frequency, such as 60 cycles, a pulsating frequency in each single-anode tube in excess of the circuit frequency cannot be readily obtained and therefore is not of any practical value.

Since this paper deals essentially with current ratings, little mention has been made of the voltage conditions in the tube. It is of course appreciated that the various factors relating to current ratings

Figure 3. Typical wave shapes of current impulses which may flow in electronic devices



may be appreciably influenced by the operating voltage, the inverse voltage, the rate of rise of the inverse voltage, and so forth. This simply means that the complete rating structure must take such influences into account where they are of importance.

If pulsations as defined by the quantities previously discussed can be repeated continuously and without overheating the tube or any of its parts, no further information on the current rating is required. Practical applications of this kind are illustrated in E_1 and G_1 of Figure 1, as in these cases the average or rms values indicated are not likely to give unsafe temperatures. On the other hand, for applications A_1 and C_1 the average currents are likely to be in excess of safe values with continuously pulsating service. However, the current values of cases A_1 and C_1 can be safely applied for intermittent pulsating service, such as illustrated by cases A_2 and C_2. In rating tubes for such service, some additional rating information must be given. The most convenient method of rating to be applied here seems to be the duty-cycle method described in AIEE report No. 1-A. However, in applying this method to the case at hand, it requires some modification. The method calls for the specification of the maximum load time per duty cycle and a time factor k indicating the maximum ratio of load time or "on" time to the total time per load cycle. Due to pulsating nature of the load current, adherence to the exact wording of report 1-A would be confusing. In Figure 1 the time T_r indicates the time of the cycles during which pulsations occur, and T indicates the time of the total number of system cycles for one duty cycle. It is obvious that confusion can be avoided by giving the maximum permissible number of pulsations per load cycle and the ratio k of these pulsations to the total number of cycles of the system frequency per load cycle. These two values must be so chosen that satisfactory operation and life of the tube is assured. (Consideration has been given to the use of the service-factor method described in report 1-A, but it was found that the use of this method would unnecessarily complicate matters for the specific case without giving any compensating advantage. This really is to be expected, because the tube ratings under discussion depend upon many factors other than the rms current, which is one of the basic values of the service-factor method.)

In some respects the considerations up to this point have been specific to vapor-filled tubes with oxide-coated filaments. However, many of the conditions are the same as for most other types of tubes, at least qualitatively. Quantitatively, there may be marked differences, because the limitations are imposed by different physical phenomena. In an ignitron, for example, tendencies toward back-fire or the possibility of losing control of the tube by the ignitor will govern the amplitude and time of the current impulses and the permissible number of impulses per load cycle; the ratio of the permissible peak currents to the average and rms values is, however, many times larger than that with oxide-coated filament tubes; again various voltage conditions may influence some of the current ratings to a considerable degree. (While some of the restricting factors for the ignitron have been mentioned merely to point out certain differences, it should not be inferred that the ignitron involves more factors for satisfactory operation than a hot-cathode tube. A complete analysis is likely to indicate that on the whole the hot-cathode tube has more restrictions and complicating factors.)

Conditions with the cold-cathode tube are somewhat peculiar, as pointed out by G. H. Rockwood.[3] He gives a curve showing that with every load current of these tubes there is a certain total life in hours of current flow. He also indicates how the life of these tubes can be calculated from his curve for other than rectangular wave shapes. With this information it is possible to calculate for any required type and amplitude of load current the percentage of idle time which must be allowed for to assure a desired total life of the tube. In this manner the time factor k can be determined. There is no need in this case to specify the maximum number of impulses per load cycle, because the "on" time can be distributed in any desirable manner over the entire life of the tube. It will thus be seen that even the cold-cathode tube, although very different from the mercury-pool or the hot-cathode tube may be fitted into the general pattern of rating structure previously described.

The method of rating single-anode tubes suggested here should enable the designer of tube circuits to apply the tubes to best advantage. It unfortunately means the assignment of a number of different ratings corresponding to a variety of practical applications encountered. However, there seems to be no way of avoiding this with the many different phenomena and considerations entering into the operation of tubes. Any attempt to pattern tube ratings after the familiar type of ratings of most electromagnetic apparatus is contrary to the basic principles involved and therefore bound to result in misapplications or uneconomical use of the devices. If single-anode tubes are used by themselves, as may be the case with half-wave rectification, it may be advisable to give in addition to the rating structure described, the rms current and possibly the average current for the various ratings to assist in the selection of conductors, protecting devices, and so forth, connected in the tube circuit.

Rating of Tube Combinations and Circuits

For many practical applications, several single-anode tubes are used in combination, and in addition other apparatus such as transformers, reactors, capacitors, resistors, and so forth, are often employed in the combination, which has either a certain output as in the case of rectifiers or inverters, or a certain carrying-capacity as in applications involving control functions. The heating of most of the additional apparatus is determined by rms current values and usually the output or carrying-capacity of the entire combination in rms values is of principal interest to the user. (There are some exceptions to this, for instance, battery chargers, where the average current value is of importance.) For these reasons, and also because most users are accustomed to rms ratings, it is usually desirable to base the ratings of the combination arrangements wherever possible on rms values unless the load is of a type requiring pulsations, such as the ignitor circuit of an ignitron. (See G_2 of Figure 1.) For cases B_1, D_1, and F_1 of Figure 1, a continuous rms rating can readily be given, and for cases B_2, D_2, and F_2 the assignment of an intermittent or load-cycle rms current rating seems to be desirable. In most applications similar to D_1 and F_1 the difference between the rms and average ratings is of little practical importance, and in cases like B_1 filters are often used and minimize the difference. In B_2, D_2, and F_2 it is necessary on account of the tube characteristics previously discussed to specify the maximum duration of the "on" period per load cycle. It thus seems simplest to complete the rating structure by merely adding the time factor k, which is also limited by the tube characteristics as previously outlined.

Whenever additional apparatus is used in the circuit combination, certain rating limitations may be imposed by this apparatus. This may at times result in the

assignment of a time factor k smaller than that called for by the tubes. For the purpose of selecting certain standard devices such as fuses, switches, and so forth, connected in series with the tube device, the rms currents corresponding to the various load-cycle ratings should be known. Although with the factor k specified, the service factor can be readily calculated, it may nevertheless be desirable to give in the rating structure the service factor in addition to the time factor k. However, the service factor here cannot, as in most other apparatus, be construed as meaning that the "on" current and the "on" time can be varied at will as long as the service factor rms value is not exceeded. (Careful study of individual types of tubes and tube combinations may disclose some cases where this interpretation can be used. For the sake of flexibility in application, it is of course desirable to at least explore in many cases the possibilities of service-factor ratings which permit variation of current and time values at least below the rated current, and preferably also above this value, within the limit of an rms rating indicated by the service factor.)

Unfortunately the load-cycle method proposed is not very flexible in its application. It restricts the application of each load-cycle rating to but a limited variety of load-cycle conditions. The time $T=T_r/k$ (in which T_r is the specified maximum "on" time per load cycle) corresponds in some ways to the so-called "averaging" time used to some extent in tube specifications. Satisfactory results will be obtained with the rated current, if there are several load cycles instead of one during the time T, as long as the ratio of the total "on" time T_r to T does not exceed the value k. Additional load-cycle ratings have to be given for applications with different current values.

It may be advisable to illustrate briefly here the rating and application method for cold-cathode tubes. Assuming, for instance, a current of 50 milliamperes in the tube described by Rockwood, the life is found to be about 42 hours a tube. Therefore, if six such tubes are used in a six-phase arrangement, giving approximately rectangular wave shapes for the impulses of each tube, the combination would have a life of about 250 hours at 50 milliamperes. This, then, simply means that if a tube combination of this type is applied in service where a life of 10,000 hours is required, the factor k should not exceed the value of 0.025, which may be entirely practical for the relay service mentioned by Mr. Rockwood. For this case the maximum "on" time per load cycle could be given as 250 hours, although this would be of no practical significance in the usual relay application having a large number of short operating periods. The principal condition is that during the 10,000-hour life the total load time does not exceed 250 hours. (This is merely given as an illustration. With the relay service under consideration by Rockwood, an arrangement of one or two tubes is more likely to be used, and in such cases the impulses will be sinusoidal.)

The examples in Figure 1 all deal with rectification. Another function of tubes is that of switching or controlling alternating current for such purposes as spot welding, for instance. The individual single-anode tube here again carries pulsations which are either sinusoidal half waves as in Figure 3B, or sinusoidal waves with delayed starts as in Figure 3D. The current handled by the usual 2-tube combination is alternating instead of direct current as in cases B_2, D_2, and F_2 of Figure 1, and the load is as a rule intermittent. The operation of the individual anodes is the same as previously discussed; the conditions with regard to multianode arrangements are different only in so far as the current ratings for the combination should be expressed in rms values of alternating current, with all other factors handled as previously indicated.

Summarizing, the rating structure suggested here comprises:

The above rating structure for continuous loads of single-anode tubes is not proposed to the exclusion of the present method of giving the average current for continuous ratings of tubes. It is intended essentially for cases where it results in more economical application of tubes than the conventional method, or where it is found more convenient. The use of the rating structure on the left side of the table is not necessarily limited to single anodes. It may also be found convenient for multianode arrangements primarily intended for supplying pulsations. If it should happen with cases G_1 and G_2 of Figure 1, for instance, that a single tube does not permit a frequency of pulsations high enough for a given application, a two-tube arrangement can be used to good advantage and may then be rated as indicated on the left side of the table. The rating structure on the right side is intended for all cases where direct or alternating currents are desired, and where pulsations or harmonics may or may not be present as an undesirable by-product of the circuits employed.

For load-cycle ratings the nominal rating is the value of the current during the load or "on" period of the load cycle. The service factor S is the ratio of the rms current for the entire time of the load cycle to the nominal rms current value during the "on" period.

Whenever tubes have a limited life, the

For Single-Anode Tubes or Each Anode of a Multianode Tube	For Combination Arrangements
A. Continuous Ratings	
1. Wave form of impulses	1. Rms nominal current rating
2. Crest value of impulses	
3. Time of impulses	
4. Frequency of impulses	
B. Load-Cycle Ratings for Intermittent Service	
Same as above supplemented by—	
5. Maximum number of impulses per load cycle (or equivalent time)	2. Maximum active time per load cycle
	3. Time factor k
6. Time factor k	4. Service factors in addition to k

In devising a system of ratings, it must be considered that under many conditions the maximum "on" time T_r per load cycle and the time factor k are interdependent. Usually k has to be decreased with an increasing value of T_r. This is always the case when heating considerations are the determining factor. (See references 4 and 5 which demonstrate this in connection with machines and other apparatus. For tubes this is more fully discussed in a companion paper by Marshall and Arnott.[6]) For this reason T_r should not be chosen any larger than necessary for practical applications. By keeping T_r low, the value of k is favorably affected.

expected life in hours must necessarily enter into the rating structure somehow, because the choice of the quantities given in the table may be materially affected by the assumed life. A typical example of this is the cold-cathode tube previously described.

As a rule, all of the quantities of the table except the service factor S, which may be given for convenience, are essential to definitely limit the application of the device to service it can handle satisfactorily. (There are a few exceptions to this, such as the cold-cathode tube where the indication of the maximum active time per load cycle is of no practical im-

portance.) However, the importance of the different quantities may vary appreciably with different tubes and applications, as may the reasons why the factors must be limited to the values given. In hot cathode tubes, for instance, the crest value of the impulses is quite a decisive value, while in many other types of tubes the maximum crest value is not such an important limitation. The maximum active time per load cycle may be primarily limited by heating considerations of various structural parts in hot-cathode tubes, while back-fire considerations or loss of proper control (also largely influenced by heating) may be most essential in some pool-type tubes. The time factor k, which determines the minimum idle periods, may be chiefly determined by heating consideration in a hot-cathode tube, while in a cold-cathode tube its value is determined essentially by the expected life of the tube. However, regardless of what these numerous variations may be, it seems that the suggested rating structure satisfies almost every conceivable condition that could be encountered in practice with continuous or intermittent service, although some exceptions to this can undoubtedly be found. It is fully realized that many factors (especially various voltage conditions) not discussed here in detail will have to be given careful consideration in the setting of current ratings. It is obvious that in the brief review of rating structures given here it would be impractical to attempt a comprehensive treatment of all phenomena involved. Some of the more important considerations not covered here will undoubtedly be pointed out in the discussion.

With the load-cycle current ratings so far discussed it has been assumed that they are intended for typical intermittent loads with definite requirements, as, for instance, the operation of relays designed for such current. Many applications like this are found in practice and can be taken care of by a single load-cycle rating. However, with many other tube arrangements, it may be desirable to give sufficient rating information to facilitate their application for a variety of intermittent service conditions. In Figure 4 the curve shown is intended to indicate the maximum "on" time permissible for a specific tube arrangement for various nominal load currents. Since it seems undesirable to list a great number of different current ratings, only a few, such as Pn_1, Pn_2, Pn_3, and Pn_4, are indicated in the figure. For each of these the maximum "on" time per load cycle can be given, as indicated by the values T_1, T_2, T_3, and T_4. In addition the value k would have to be given for each case. This then would make possible the application of the arrangement within its range of satisfactory performance for different applications, as intermediate values could be readily interpolated. A question arises, however, as to how a single nominal commercial rating should be selected for such a condition, especially for service where any of the loads are at times applied for considerable periods, as may occur with spot-welding arrangements for instance. There is not much choice between the different ratings in selecting one as a nominal commercial rating under these circumstances. The continuous rating P is so far from representing actual service conditions that it would not be a very satisfactory nominal rating. The maximum rating P_m is not very well defined because the time T_5 in Figure 4 may range from one cycle in the case of spot welding to the conventional period of one minute used in connection with the larger rectifier equipment.

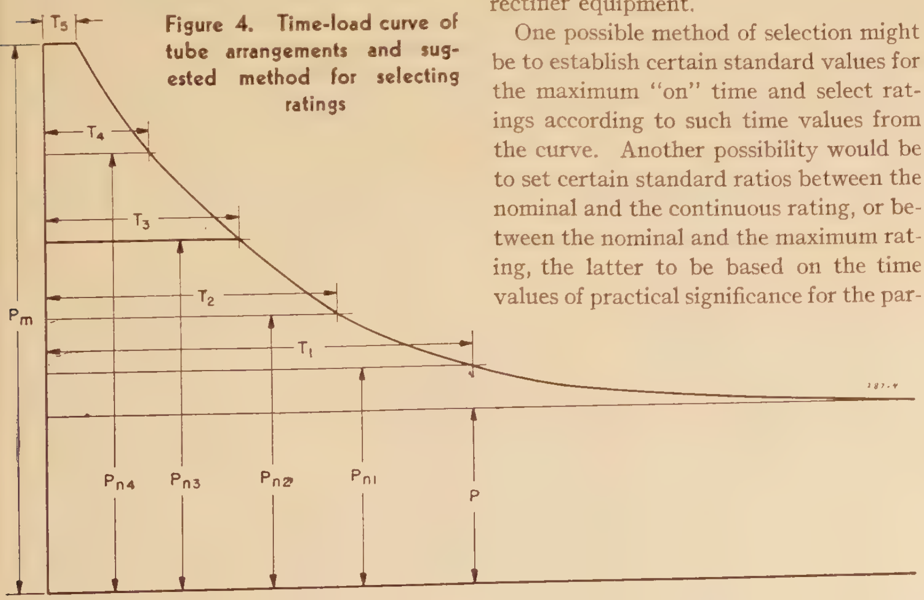


Figure 4. Time-load curve of tube arrangements and suggested method for selecting ratings

One possible method of selection might be to establish certain standard values for the maximum "on" time and select ratings according to such time values from the curve. Another possibility would be to set certain standard ratios between the nominal and the continuous rating, or between the nominal and the maximum rating, the latter to be based on the time values of practical significance for the particular type of tube or service, such as one cycle for spot-welding arrangements and one minute for large rectifying equipment. Some measure of uniformity could be accomplished by always using preferred numbers for these ratios, as indicated in Figure 4, where all the steps between the different ratings have been selected to be 25 per cent, corresponding to the coefficient of the ten series of preferred numbers. In this case the load Pn_3, which is about halfway between the continuous and short-time maximum rating, has been indicated by a heavy line to serve as the nominal commercial rating. It would seem highly desirable to devise some standard method for settling some of these questions, as it would greatly assist in the gradual evolution of a reasonably uniform method for electronic devices.

As previously stated, the suggestions given here are merely preliminary; final recommendations should not be given until considerably more work has been done in analyzing the various conditions. However, it is hoped that the brief outline will stimulate others to investigate various practical arrangements and applications for the purpose of determining whether the suggestions made here can be used, or whether they can be altered in some way to make them even more generally applicable. It is obvious that no system of rating applicable to all conditions can be devised; however, it seems entirely possible to develop a system capable of broad application and requiring only slight modifications for specific cases. At any rate, it should be possible to at least establish a nomenclature defining such terms as time factor, service factor, and so forth, which will be generally applicable and definite in its interpretation in all applications.

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Ignitor Excitation Circuits and Misfire Indication Circuits

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Synopsis: Several circuits are available for excitation of ignitron rectifiers. Static magnetic-impulse circuits are used for most applications. Several such circuits are discussed in some detail. Ignitor requirements are given, and circuit requirements affecting the choice of excitation circuits are indicated.

While ignitor failures are rare, they should be detected promptly to utilize all apparatus to the best advantage. Two conditions may arise

1. When two anodes operate in parallel from a single transformer winding.

2. When a single anode is connected to each transformer winding.

Means for detecting ignitor misfiring are described for both conditions.

An ignitron is an electric valve in which the cathode spot or electric arc is established every cycle at the beginning of the conduction period of the main anode and is allowed to extinguish

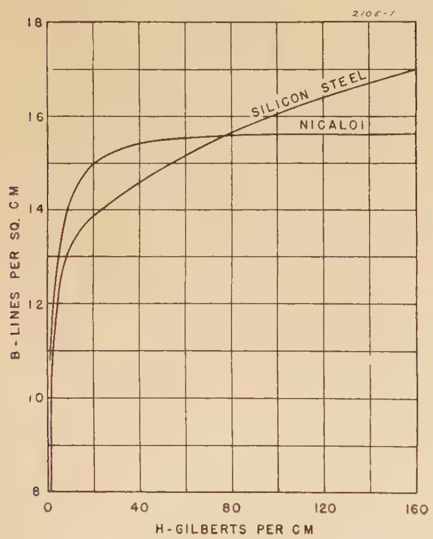


Figure 1. D-c magnetization curves

at the end of the conduction period. The arc is established by means of an ignitor, which is an element made of a high-resistance material and partly submerged in the cathode mercury pool. The ignitor is excited or fired by passing an electric current of sufficient strength through it into the cathode to establish a cathode spot or arc. Static magnetic-impulse excitation circuits have certain definite advantages over other ignitor excitation circuits.[1,2] The circuits here described satisfy certain operating conditions.

As current is built up in the ignitor to the firing value, power is consumed by the ignitor. At the instant the cathode spot appears most of the ignitor current transfers to the electric arc instead of passing through the whole length of the ignitor to the cathode, and the voltage across the ignitor suddenly collapses to arc-drop value. It is therefore desirable to build up the ignitor current rapidly to the firing point to limit the losses and the heating of the ignitor. Further, since the ignition current varies considerably from cycle to cycle, a rapid build-up of ignitor current will result in a more uniform firing angle.

Magnetic-Impulse Excitation Circuits

To obtain a rapid build-up of current or peaked current wave, a nonlinear reactor is used

In the high impedance region of such a reactor, that is, below its saturation point, there is substantially no ignitor current. As the impedance decreases, the rate of change of ignitor current increases. An abrupt transition to a peaked current wave requires a sharp knee in the magnetization curve of the reactor; a rapid rise of current requires a low saturated reactance. The use of an alloy such as Nicaloi (see Figure 1) and a well-interleaved core construction result in

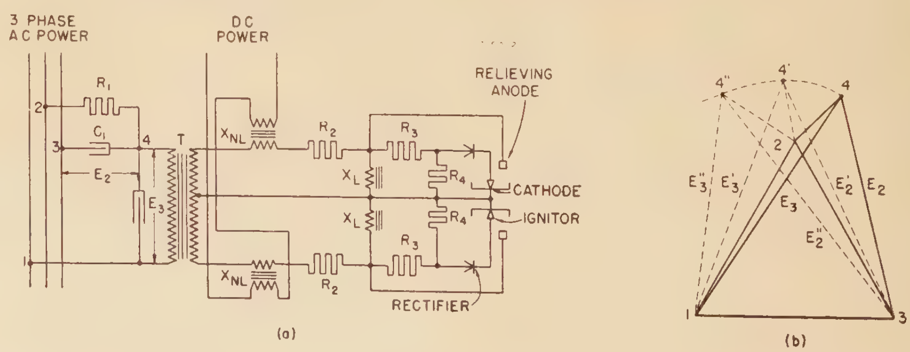
1. Sudden initiation of impulse current.

2. A maximum ratio of unsaturated to saturated reactance values. A capacitor may be used as a storage reservoir so that other circuit reactance will not reduce the rate of rise of the firing impulse current.

In Figure 2 is shown a magnetic-impulse ignitor excitation circuit. Here a nonlinear reactor X_{NL} is used for each ignitor. This is necessary, because the reactor is biased with d-c ampere-turns to obtain a high peak current in the a-c winding in one direction but not in the other. The high peak current is allowed to pass through the ignitor in the positive direction, that is, from ignitor to cathode, while the low negative current is blocked by a dry-plate rectifier in series with the ignitor and instead flows through the linear reactor X_L. This circuit gives a peaked current wave having a base 60 electrical degrees long, that is, the arc is maintained for 60 electrical degrees of the fundamental frequency. In order to relieve the ignitor and the rectifier in series with the ignitor of this current after the arc has been established, a relieving anode and transfer resistor R_3 are used. The resistance R_4 reduces the inverse voltage impressed on the rectifier. The resistance R_2 limits the current in the circuit. Voltage control of the d-c output of an ignitron rectifier can be obtained by retarding the firing of the ignitors. The capacitors and resistance connected between the power supply and the primary of the transformer T, together with the amount of d-c saturation of the nonlinear reactors X_{NL}, permit voltage control by shifting of the ignitor firing.

The vector diagram shows how this shifting of phase is accomplished. Vector 1-4 is the voltage impressed on the primary of the transformer, and vector 4-3 the voltage impressed across the condenser C_1, and triangle 1, 2, and 3 represents the three-phase supply voltage. As the saturating current in the nonlinear reactors is increased, the point 4 travels

Figure 2. Ignitor excitation circuit using d-c biased nonlinear reactors



(a) Circuit diagram

(b) Vector diagram

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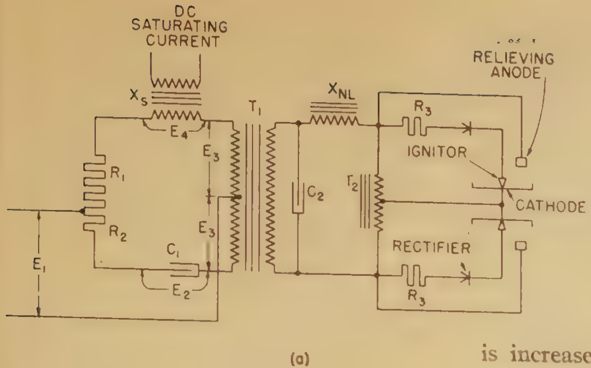
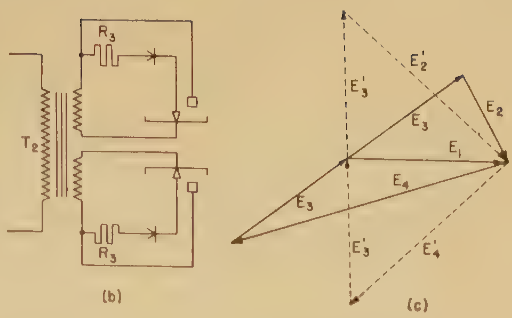


Figure 3. Ignitor excitation circuit using saturable reactor X_s for controlling phase of firing

(a) Circuit diagram
(b) Modified circuit diagram
(c) Vector diagram



on the locus, 4, 4′, and 4″. Thus the voltage impressed on the transformer advances as the saturating current is increased, and therefore the angle of ignitor firing advances with increasing saturating current. The purpose of resistance R_1 is to hold the amplitude of the voltage E_3 more nearly constant. The advantage of this firing circuit is the long period over which the arc is maintained.

Figure 3 shows an excitation circuit in which one nonlinear reactor X_{NL} is used to fire two ignitors. The nonlinear reactor is allowed to saturate in both directions, thus giving peak currents in both directions. An autotransformer T_2 is used to provide a return path for the ignitor current from the cathodes of the ignitrons. In case the cathodes must be insulated from each other, this can be done by making the autotransformer an insulating transformer as shown in Figure 3b. The shifting of the angle of ignitor firing in this case is accomplished by a saturable reactor X_s, together with a capacitor C_1. The resistances R_1 and R_2 keep the voltage wave across the transformer T_1 symmetrical and in this way insure that the two ignitors will fire 180 electrical degrees apart. Capacitor C_2 helps to increase the peak volt-amperes delivered to the ignitors.

In the vector diagram, E_3 is the voltage impressed on the transformer T_1 with low saturating current in the saturable reactor, E_2 is the voltage across the capacitor C_1, E_4 is the voltage across the a-c winding on the saturable reactor, and E_1 is the line or supply voltage. As saturating current in the saturable reactor is increased, the a-c voltage across the saturable reactor decreases, and the voltage across the capacitor C_1 increases as shown by the dotted vectors. The voltage E_3 on the transformer advances from E_3 to E_3' and consequently the ignitor firing advances. The voltage drops across the resistances R_1 and R_2 are neglected to simplify the diagram. In this firing circuit the arc is maintained for about 25 degrees. The advantages of this circuit over the one shown in Figure 2 are that the power consumed is less and the apparatus is smaller.

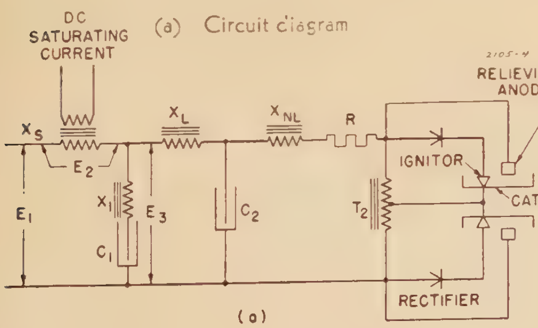
Figure 4 shows still another type of excitation circuit which shortens up the period over which the arc is maintained to about 18 degrees. This reduces the power drawn by the circuit still further, being only about 200 watts per ignitor. A modification of the autotransformer similar to Figure 3b will provide for insulated cathodes. The phase-shifting network consists of a saturable reactor X_s, a linear reactor X_1, and a capacitor C_1.

The vector diagram shows how the voltage E_3 varies in phase, with substantially constant amplitude as the voltage vector E_2, which is the a-c voltage across the saturable reactor, is changed in amplitude by means of the d-c saturating winding. The angle of firing of the ignitors shifts with the angle of the vector E_3.

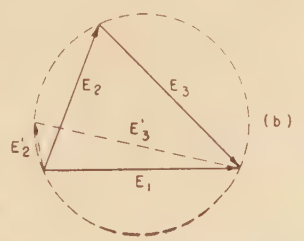
Figure 4c shows the current in the nonlinear reactor, the voltage impressed on the ignitor, and the supply voltage.

Sometimes it is desirable to operate the main anodes of two ignitrons in parallel, and in that case the ignitors must be fired simultaneously in order that the main anodes will divide the current equally. This is best accomplished by firing four ignitors from one ignitor excitation circuit as shown in Figure 5. The current dividing autotransformer T_3 causes the two ignitors connected to the ends of the same transformer to be fired practically simultaneously. If the other transformers, reactors, resistors, and capacitors in the excitation circuit are built for the same voltage, but twice the current rating, we will have a circuit that is twice the size in kilovolt-ampere rating and will draw twice as much current and power from the line to fire twice as many ignitors. However, it has been found that the peak volt-amperes that this circuit is capable of delivering to a hard firing ignitor have been practically doubled. There is therefore a distinct advantage when operating ignitrons in parallel in firing their ignitors by this arrangement.

In order that various kinds of ignitor excitation circuits can be compared, it is necessary to have some way of measuring the peak voltage and current that the circuit is capable of delivering to the ignitor. In a circuit designed to use relieving anodes a variable resistance is connected in series with one ignitor between the ignitor and the blocking rectifier. In case the circuit is designed to operate without relieving anodes, a relieving anode should be used during this test connected as shown in Figure 6. This variable resistance can then be considered as being part of the ignitor, and the two together represent a high-resistance ignitor. The amount of current required to fire the ignitor can be varied by changing the depth of immersion of the ignitor in the cathode. With deep immersion a high current is required, and with shallow immersion a low current. For each different ignitor immersion depth, the resistance R is increased to such a value

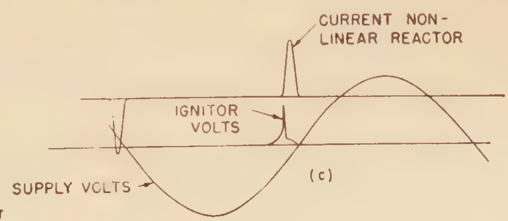


(a) Circuit diagram



(b) Vector diagram

Figure 4. Low-loss ignitor excitation circuit



(c) Voltage and current waves

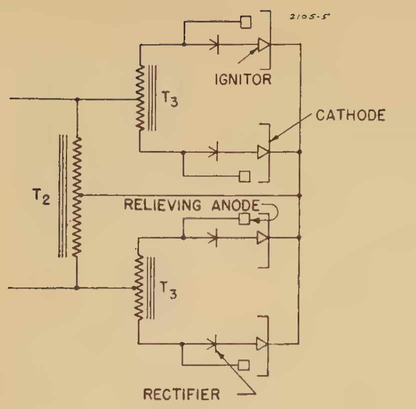


Figure 5. Method of firing two ignitors in parallel

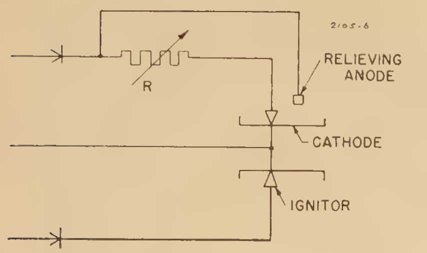


Figure 6. Circuit for measuring output of excitation circuit

that the ignitor misfires occasionally. With this setting the peak voltage across the resistance and ignitor is measured during a misfire. This is the peak voltage the circuit is capable of delivering simultaneously with the peak current. The peak current is determined from the voltage across R. A curve can then be plotted showing the peak current and the peak voltage the circuit is capable of delivering to the ignitor. Such curves are shown in Figure 7. A circuit test made by substituting a resistance for the ignitors can give misleading results, because a resistance does not have the nonlinear characteristic of an ignitor at the instant of firing.

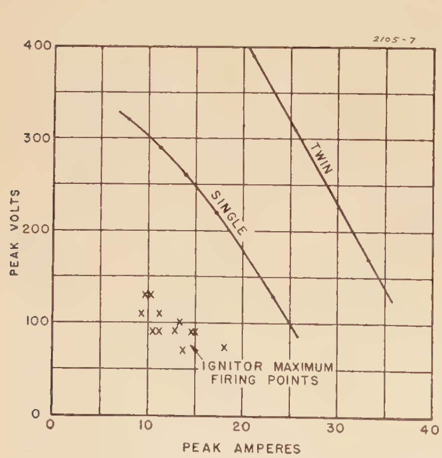


Figure 7. Volt-ampere output curves of excitation circuit Figure 4 and modification as in Figure 5, with maximum firing points of typical ignitors

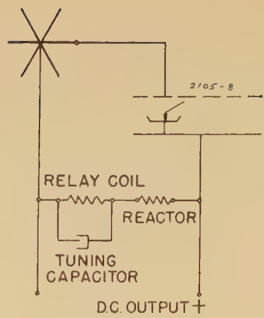


Figure 8 (left). Detection of ignitor misfire by fundamental - frequency component in d-c ripple

Figure 7 shows the peak volts and amperes that a typical circuit as in Figure 4 is capable of delivering to the ignitors. The curve labelled "single" is for the circuit as shown in Figure 4 and the curve labelled "twin" is for the circuit in Figure 4 when modified according to Figure 5 and using apparatus with twice the current rating. The points indicated by crosses in Figure 7 represent maximum peak voltage and current to fire typical ignitors. This shows that the circuit is putting ample power into the ignitors.

Detection of Excitation Failure

During operation of an ignitron rectifier one or more anodes may fail to conduct, due to a fault in the ignitor firing circuit or in one of the ignitors. Failure may be occasional or persistent.

Persistent misfire of one anode in a rectifier where two anodes are operating in parallel results in increased load being carried by the companion anode with a corresponding increase in arcback probability. In other circuits misfire of an anode may result in increased duty on one or more of the other anodes, with

1. Corresponding greater arcback probability.
2. Increase in transformer heating.

In any case, an indication of faulty operation is desirable, so that the fault may be remedied and normal operation continue.

Excitation failure may be detected by:

1. Action of the power circuit.
2. Action of the excitation circuit.
3. Action of auxiliary electrodes in the rectifier.

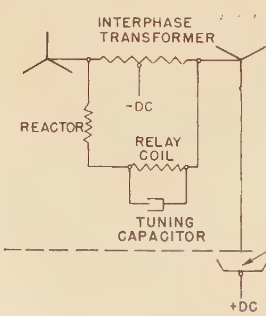


Figure 9 (left). Detection of ignitor misfire by fundamental - frequency component in interphase transformer voltage

I. Detection of Excitation Failure by Action of the Power Circuit

1. If one anode of a rectifier fails to fire, there will be a component of supply frequency in the d-c voltage ripple. The magnitude of this component will depend on the number of phases of the rectifier and the nature of the load. A relay connected to the d-c output voltage and having its coil tuned to the supply frequency will indicate misfire. This circuit is shown in Figure 8. A variation of this arrangement is to connect the tuned relay across a d-c reactor in series with the load. Either arrangement is most effective when the number of phases is small, and the d-c load is fairly reactive.

2. When an interphase transformer is included in the rectifier circuit, a voltage component of supply frequency occurs across the interphase transformer during misfire. A relay or other indicating device tuned to the supply frequency and connected across the interphase transformer will indicate the presence of a missing anode. The connections are shown in Figure 9. In a quadruple-wye circuit, an induction relay with two tuned coils may be used, each coil being connected to one of the 180-cycle interphase transformers.

3. Figure 10 shows a circuit which utilizes the increased positive voltage between anode and cathode, when the anode does not fire. This arrangement is most suitable for rectifiers operating without phase retard.

4. Figure 11 shows a circuit where unbalance between the neutral currents of a double-wye rectifier saturates a reactor and decreases its impedance.

5. Missing of an anode in one wye of a double-wye rectifier generally results in a disproportionate decrease of current in the other anodes of that wye. This is the basis of the circuit in Figure 12, which is affected by the difference between anode currents in the two wyes.

6. The missing of one anode will result in unequal currents in the supply line. This

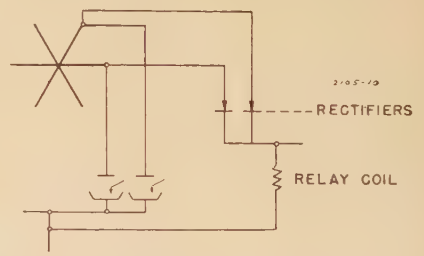
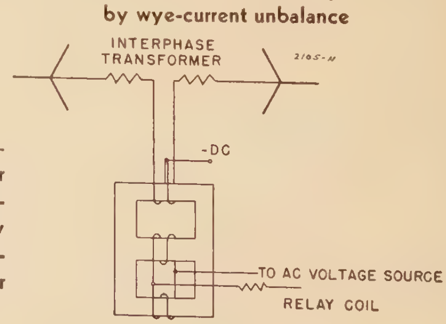


Figure 10. Detection of ignitor misfire by positive voltage of missing anode

Figure 11 (below). Detection of ignitor misfire by wye-current unbalance



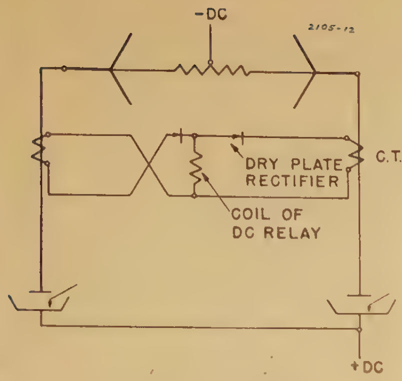


Figure 12. Detection of ignitor misfire by anode-current unbalance

may be recognized by visual observation of line ammeters or by a negative-phase-sequence relay. Operation of a double-wye rectifier circuit on five anodes results in a large negative-phase-sequence second harmonic component, due to the unbalance between the two secondary wyes. A negative-phase-sequence relay is less useful for misfire detection as the number of phases is increased.

7. When two anodes are operated in parallel from the same transformer winding, it is possible to utilize the current-dividing reactor, if one be used, as an indicator of misfire. Failure of an anode to fire would result in a much higher voltage than normal across the current-dividing reactor. This voltage may be utilized to operate an indicating device. Another method of using the current-dividing reactor is to design the reactor so that the core acts as the magnet of a relay and design the relay structure so that there will be sufficient flux to attract the armature only when the unbalance between the anode currents corresponds to misfire of one anode at 50 per cent or more load on the rectifier. Figure 13 is a photograph of such a reactor.

8. Another method of detecting misfire in the case of parallel anodes is to connect in each anode lead a current transformer designed to permit open circuit operation. If the secondaries are connected in opposition, there will be no voltage when both anodes fire, but a resultant voltage if one is missing. This may be used to operate a suitable relay. The resultant voltage will be present if all the pairs of oppositely connected secondaries are joined in series.

It should be noted that the above schemes for detecting missing of one of a pair of parallel anodes will not detect simultaneous missing of both anodes. Such missing will require one of the previously described circuits.

9. When a rectifier is carrying a steady

Figure 13. Combined current-dividing reactor and misfire-detection relay

load, as in electrolytic processes, and anodes are not operated in parallel, missing of an ignitor may be detected by observation of the d-c ammeter in the output circuit of the rectifier. This will show a distinct flicker each time misfire occurs. The anode transformer also gives an audible indication of misfire.

II. Detection of Excitation Failure by Action of the Excitation Circuit

Failure of an element of the excitation circuit may result in one of a number of conditions, depending upon the nature of the failure. Since the ultimate purpose of failure detection is to determine if an ignitor is missing, the preferred location of a misfire device is in the circuit of the ignitor itself. Such a device becomes involved as it must function during

1. Ignitor short circuit.
2. Ignitor open circuit.
3. Insufficient power to fire ignitor.

Moreover, there must be one such device for each ignitor. Action of some other circuit than the excitation circuit appears desirable for misfire detection.

III. Detection of Excitation Failure by Action of Auxiliary Electrodes in the Rectifiers

The ignitron rectifier cell usually has an anode baffle which is excited from a different source than the other electrodes. Since current cannot flow in the baffle circuit unless a cathode spot is formed, current indicating means in series with the baffle or voltage indicating means across the baffle resistor will detect misfire.

Location of Missing Ignitor

With the exception of the scheme just described for indicating the presence of baffle current, the above misfire indication circuits do not indicate the specific ignitor which is misfiring. If an oscilloscope is at hand, missing may be located by observation of ignitor voltage. A clampon or tong ammeter may be used to detect current in the main anode. If a low-reading tong ammeter is available, it may be used to measure the current in the baffle. A compass held near an anode bus will show when an anode is not firing.

Conclusion

Static magnetic-impulse excitation circuits have definite advantages in many applications of ignitron rectifiers. Three circuits which have been described have different modes of operation, and their choice is determined by operating requirements. Excitation circuits are generally designed with an ample margin of power over ignitor requirements and in twin circuits for parallel operation. This margin is automatically increased for hard-firing ignitors.

An ignitor may stop firing even with ample excitation power. This condition may be detected in a number of ways. A choice of methods is available for specific applications.

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Formulas for Calculating Short-Circuit Stresses for Bus Supports for Rectangular Tubular Conductors

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THE mechanical forces acting on bus-bar supports during short circuit are determined in some measure by all of the following:

The magnitude and decrement factor of the short-circuit current.

The mutual electromagnetic forces exerted among the individual conductors.

The natural frequencies, motional resistance, and physical arrangement of the bus-bar structures.

A theoretical treatment, substantiated by experimental data, of the individual and collective effects of these factors on the magnitude of bus-bar support stresses and a method of calculating numerically these effects are to be found in a comprehensive paper by Schurig and Sayre.[1] In shorter articles based on the just mentioned paper Schurig, Frick and Sayre,[2] Tanberg,[3] and Specht[4] have presented certain charts, nomograms, and short-cut methods facilitating the numerical calculations involved in an actual problem. However, regardless of the method of calculation adopted, a determination of bus-bar support stress yet requires calculation of the electromagnetic forces exerted on the conductors carried on the support.

The mutual electromagnetic force exerted between two isolated, long nonpermeable conductors, spaced D inches between their parallel axes and carrying direct currents of I and I' amperes, is commonly calculated from the formula

$$f=(5.4kII'/D)10^{-7} \text{ pounds per foot of bus length} \quad (1)$$

or, if absolute units are used, from

$$f=2kII'/D \text{ dynes per centimeter of bus length} \quad (2)$$

Herein k is a parameter, a variously named constant, of magnitude determined by the geometry and relative position of the two conductor cross sections. In a group of conductors the force acting on any one conductor is calculated by using equation 1 to compute in turn the force exerted on the conductor by each of the remaining conductors and then adding these forces vectorially. If alternating instead of direct currents flow in the conductors, then providing f, I, and I' are taken as instantaneous values, and providing skin and proximity effects are negligible—this latter condition is commonly satisfied if rectangular tubular conductors are used and is sufficiently satisfied for strap conductors if, as is usual, the bus bar considered is either a single nonlaminated strap or is constructed of properly transposed laminated conductors—equation 1 is yet used, and k is the same as for direct currents. Accordingly, simple arithmetic aside, determination of the *electromagnetic* force acting on a given support reduces, in essence, to determination of the needed values of k.

Of essential interest to the designer of polyphase bus structures are the values of k for a group of identical conductors of solid or tubular rectangular cross section with axes in a plane. The formula for k for conductors of *solid* rectangular cross section was derived a quarter of a century ago; as regards rectangular tubular conductors, no formula for k seems to have been published hitherto. Yet as the mechanical and electrical advantages stemming from use of this cross section have resulted in an extensive and currently increasing use of it for feeding heavy current loads—to be noted in particular is its use in new bus installations supplying batteries of welders (in airplane, tank, and automobile factories) and banks of electric furnaces (for alloy and tool steels, aluminum, and various other metals) necessitated by the defense program—it is most desirable to have formulas with which one can quickly calculate the value of k for any conductor spacing or rectangular cross section. Such formulas are derived in this paper.

It is assumed that the conductors are nonmagnetic, right-cornered, and of such length that end effects are negligible, and that they carry currents uniformly distributed over the cross sections of the individual conductors. Of these assumptions all but that of right-corneredness are consistent with or are closely approximated in current practice. But although rounded corners are found on rectangular tubular conductors—to minimize "edge effect"—treating them as right-cornered introduces error negligible with reference to the accuracy required in calculating bus-bar stresses and enables solution of an otherwise intractable problem.

As regards values of k for strap conductors (which usually have right corners), the formulas derived in this paper—a strap conductor is a special case of a rectangular tubular conductor—offer an attractive alternative to the means of determination now used: curves, found in the references previously cited and in most electrical handbooks, these being plotted originally by Professor H. B. Dwight[5] from the above-mentioned formula derived by him. It is illustrated in section III that from equations 17 or 18 one can calculate rapidly, with but *pencil* or *slide rule*, the value of k for two symmetrically located, identical strap conductors.

The mentioned formulas for solid and tubular rectangular conductors obtained, ease and rapidity of use are illustrated by the numerical solution of several problems typical of bus design.

I. The Formal Solution for k

As derived in a previous paper by the writer,[6] the electromagnetic energy associated with a unit length of an isolated circuit constructed of two identical rectangular tubular conductors, symmetrically located as in Figure 1, is

$$W=w^2[W(r, s)+W(R, S)+2W(t, S+t)+2W(R+t, t)-2W(R+t, S+t)-2W(t, t)] \quad (3)$$

where, typically, $W(r, s)$ is defined by

$$W(r, s)=(1/6)[2F(D, s)-F(D+r, s)-F(D-r, s)+2F(r, s)-2F(0, s)+4\pi r^2s(3D-r)] \quad (4)$$

and, typically, $F(r, s)$ is defined by

$$F(r, s)=(r^4-6r^2s^2+s^4)\log(r^2+s^2)^{1/2}+4rs(r^2-s^2)\tan^{-1}(r/s)-r^4\log|r| \quad (5)$$

All units are in the absolute system: the current density w in abamperes per square centimeter; D, r, and s in centimeters; W in ergs per centimeter of line length.

As is well-known, the mutual electro-

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magnetic force exerted between the two conductors is given by

$$f=\partial W/\partial D \text{ dynes per centimeter of bus length} \quad (6)$$

Equating equations 2 and 6 and solving for k yields

$$w^2k=[D/2(rs-RS)^2]\partial W/\partial D \quad (7)$$

Substituting equation 3 in equation 7 gives

$$k=[D/2(rs-RS)^2]\left[\frac{\partial W(r,s)}{\partial D}+\frac{\partial W(R,S)}{\partial D}+2\frac{\partial W(t,S+t)}{\partial D}+2\frac{\partial W(R+t,t)}{\partial D}-2\frac{\partial W(R+t,S+t)}{\partial D}-2\frac{\partial W(t,t)}{\partial D}\right] \quad (8)$$

From equation 4 we have

$$\frac{\partial W(r,s)}{\partial D}=(1/6)\left[\frac{\partial}{\partial D}\{2F(D,s)-F(D+r,s)-F(D-r,s)\}+12\pi r^2 s\right] \quad (9)$$

The bracketed expression in equation 9, considered as a function of r alone, can be expanded in a power series about the point $r=D$. Substituting this expansion in equation 9 we have

$$\frac{\partial W(r,s)}{\partial D}=(1/6)\left[\frac{\partial}{\partial D}\left\{-r^2\frac{\partial^2F(D,s)}{\partial D^2}-\frac{r^4}{2\cdot 3!}\frac{\partial^4F(D,s)}{\partial D^4}-\frac{r^6}{3\cdot 5!}\frac{\partial^6F(D,s)}{\partial D^6}-\ldots\right\}+12\pi r^2 s\right]$$

$$=(1/6)\left[-r^2\frac{\partial^3F(D,s)}{\partial D^3}-\frac{r^4}{2\cdot 3!}\frac{\partial^5F(D,s)}{\partial D^5}-\frac{r^6}{3\cdot 5!}\frac{\partial^7F(D,s)}{\partial D^7}-\ldots+12\pi r^2 s\right] \quad (10)$$

The details of the expansion and expressions for the various derivatives, in terms of the dimensions and spacing of the conductors, are given in the appendix. Substituting appropriately in equation 10 and collecting terms, we have

$$\frac{\partial W(r,s)}{\partial D}=2r^2\left[[2s\tan^{-1}(s/D)-D\log\left(1+\frac{s^2}{D^2}\right)]+\frac{r^4}{3}\frac{s^2}{D(D^2+s^2)}+\frac{r^6}{45}\left[\frac{1}{D^3}+\frac{-D^3+3Ds^2}{(D^2+s^2)^3}\right]+\ldots\right.$$
$$=2DM(r,s) \quad (11)$$

where $M(r, s)$ is defined by

$$M(r,s)/r^2=2m\tan^{-1}m-\log(1+m^2)+\frac{n^2m^2}{6(1+m^2)}+\frac{n^4}{90}\left[1+\frac{-1+3m^2}{(1+m^2)^3}\right] \quad (12)$$

where $m=s/D$ and $n=r/D$.

Substituting equation 11 in equation 8 yields

$$k=[D^2/(rs-RS)^2][M(r,s)+M(R,S)+2M\times(t,S+t)+2M(R+t,t)-2M(R+t,S+t)-2M(t,t)] \quad (13)$$

If $R=S=t=0$, we have solid rectangular conductors, and equation 13 becomes

$$k=(1/m^2r^2)M(r,s) \quad (14)$$

Having determined k—note that k is a dimensionless quantity; thus D, r, and s can be measured in any unit of length—the electromagnetic force f can be determined from equation 1.

II. Approximate Expressions for M(r, s)

In most calculations the conductor cross sections and spacing are such that in equation 11 the terms involving powers of n are negligible. If so, equation 12 reduces to

$$M(r,s)/r^2=2m\tan^{-1}m-\log(1+m^2) \quad (15)$$

Although equation 15 can be calculated directly by the aid of appropriate tables or by use of a slide rule, it is more desirable to have $M(r, s)/r^2$ expressed directly in powers of m. Accordingly, substituting in equation 15, the usual power series for $\tan^{-1} m$ and for $\log(1+m^2)$ and collecting terms, we have the more desirable form

$$M(r,s)/r^2=m^2-m^4/2\cdot 3+m^6/3\cdot 5-m^8/4\cdot 7+\ldots \quad (16)$$

where $m<1$.

Substituting equation 16 in equation 14 we have for solid conductors

$$k=1-m^2/2\cdot 3+m^4/3\cdot 5-m^6/4\cdot 7+\ldots \quad (17)$$

where $m<1$.

Occasionally, in equation 12 the term in n^2 is not negligible in comparison with the first two terms. In this case, expanding $(1+m^2)^{-1}$ in powers of m and adding to equation 16 we have

$$M(r,s)/r^2=m^2\left(1+\frac{n^2}{6}\right)-m^4\left(\frac{1}{6}+\frac{n^2}{6}\right)+m^6\left(\frac{1}{15}+\frac{n^2}{6}\right)-\ldots \quad (18)$$

where $m<1$.

III. Some Illustrative Examples

Example 1

Conductors a, b, c of a three-phase bus are of four- by one-half-inch strap, spaced six inches between adjacent axes, with four-inch edges perpendicular to the plane of axes. To calculate k for conductors a and b, and for conductors a and c.

Part A. $D=6$ inches; $r=0.5$ inch; $s=4$ inches; $n=0.5/6=1/12$; $m=2/3$. From equation 12 for the terms taken in order

$$M(r,s)/r^2=(4/3)\tan^{-1}(2/3)-\log(13/9)+1/2808+\ldots$$
$$=0.7840-0.3677+0.0004=0.4167$$

Accordingly, from equation 14

$$k=(9/4)(0.4167)=0.938$$

For comparison, we have from equation 17

$$k=1-4/54+16/(81)(15)-64/(729)(28)+256/(6561)(45)-\ldots$$
$$=1-0.0741+0.0132-0.0031+0.0009=0.937$$

Part B. $D=12$ inches; $r=0.5$ inch; $s=4$ inches; $n=0.5/12=1/24$; $m=1/3$. As n and m are smaller even than in part A, k is best calculated directly from equation 17.

$$k=1-1/54+1/(81)(15)-1/(729)(28)+\ldots$$
$$=1-0.0185+0.008=0.982$$

These two values of k computed from equation 17—with a ten-inch slide rule and with purposely exaggerated accuracy—indicate the rapidity with which this expression yields values of k for strap conductors. As in practice but the first two digits are used in actual computations, but the first two or three terms in equation 17 need normally be calculated.

Example 2

Conductors a, b, c of a three-phase bus are 2.5- by 2.5-inch square tubular conductors, 0.5 inch thick, spaced 10 inches between adjacent axes. To calculate k for conductors a and b

$D=10$ inches; $r=s=2.5$ inches; $R=S=1.5$ inches; $t=0.5$ inch. For the data given we have from equation 13

$$k=(100/16)[M(2.5,2.5)+M(1.5,1.5)+2M(0.5,2)+2M(2,0.5)-2M(2,2)-2M(0.5,0.5)] \quad (19)$$

To determine k each of the terms of equation 19 must be calculated. From equation 18 we have for the terms taken in order

$$M(2.5,2.5)=6.25(0.063151-0.000692+0.000019)=0.39049$$
$$M(1.5,1.5)=2.25(0.022584-0.000086)=0.05062$$
$$M(0.5,2)=0.25(0.040017-0.000267+0.000004)=0.00994$$
$$M(2,0.5)=4.00(0.002517-0.000001)=0.01006$$
$$M(2,2)=4.00(0.040267-0.000277)=0.15996$$
$$M(0.5,0.5)=0.25(0.002501-0.000001)=0.00063$$

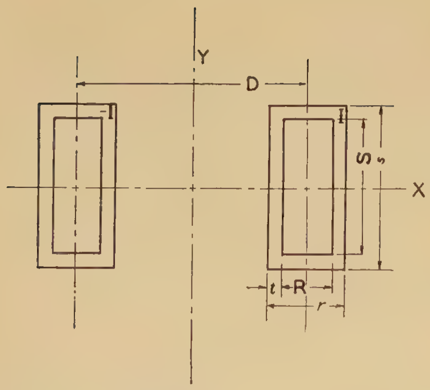

Figure 1. Rectangular tubular conductors symmetrically located

Substituting these values in equation 19 we have for k

$$k=(100/16)[0.39049+0.05062+0.01988+0.02012-0.31992-0.00126]$$
$$=0.999+$$

This example is of particular interest, as, in addition to illustrating the method of calculating k for rectangular tubular conductors, it reveals that for *square tubular conductors*—and these are commonly used—*the value of k for practical conductor spacings is unity.* If the conductors are rectangular with long sides perpendicular to the plane of axes, k will be less than unity; and if the short sides are perpendicular to the plane of axes, k will be greater than unity: in either case k can be calculated as above.

Appendix

Considered as a function of r alone

$$F=2F(D,s)-F(D+r,s)-F(D-r,s)$$

can be expanded in a power series about the point $r=D$. Accordingly, by Taylor's theorem we have

$$F=-r^2\frac{\partial^2F(D,s)}{\partial D^2}-\frac{r^4}{2\cdot 3!}\frac{\partial^4F(D,s)}{\partial D^4}-\frac{r^6}{3\cdot 5!}\frac{\partial^6F(D,s)}{\partial D^6}-\ldots$$

where

$$F(D,s)=\tfrac{1}{2}(D^4-6D^2s^2+s^4)\log(D^2+s^2)-D^4\log D+4(sD^3-Ds^3)\tan^{-1}(D/s)$$

$$\frac{\partial F(D,s)}{\partial D}=(2D^3-6Ds^2)\log(D^2+s^2)-4D^3\log D+4(3sD^2-s^3)\tan^{-1}(D/s)-3Ds^2$$

$$\frac{\partial^2F(D,s)}{\partial D^2}=6(D^2-s^2)\log(D^2+s^2)-12D^2\log D+24Ds\tan^{-1}(D/s)-7s^2$$

$$\frac{\partial^3F(D,s)}{\partial D^3}=12D\log(D^2+s^2)-24D\log D+24s\tan^{-1}\times(D/s)$$
$$=12D\log(D^2+s^2)/D^2-24s\times\tan^{-1}(s/D)+12\pi s$$

$$\frac{\partial^4F(D,s)}{\partial D^4}=12\log(D^2+s^2)-24\log D$$

$$\frac{\partial^5F(D,s)}{\partial D^5}=-\frac{24s^2}{D(D^2+s^2)}$$

$$\frac{\partial^6F(D,s)}{\partial D^6}=24\left[\frac{1}{D^2}-\frac{D^2-s^2}{(D^2+s^2)^2}\right]$$

$$\frac{\partial^7F(D,s)}{\partial D^7}=48\left[-\frac{1}{D^3}-\frac{-D^3+3Ds^2}{(D^2+s^2)^3}\right]$$

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The Carbon Arc—a Valuable Industrial Tool

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Synopsis: The versatility of the carbon arc as a source of both visible and invisible radiation is shown to be due, in part, to the three basic types of operation to which it is adapted and also to the fact that the character of its energy emission can be modified by changes in core composition. It is used in its various forms for many photochemical and irradiation processes, some of which require specific bands of ultraviolet radiation or close reproduction of the effects of natural sunlight. The carbon arc is preferred in other instances, because the optical requirements of the application necessitate a light source of small area and extreme brilliancy. Several industrial and commercial uses are cited together with the characteristics which give preference to the carbon arc as a source of radiation.

THE present widespread industrial use of the carbon arc is due to certain characteristics or combination of characteristics in which it is superior to other available sources of artificial radiation. Capable of efficiently producing a large volume of light, the original commercial application of the carbon arc was in the field of general illumination. It has been used extensively for street lighting and for the illumination of factories, auditoriums, and other large areas. Its present value is due, in large measure, to its versatility as a source of invisible as well as visible radiation and extends into many fields other than those of general illumination. In some instances preferred solely for its visible radiation, the carbon arc is selected for other applications because of the character and intensity of its invisible rays, while the choice at times may be due to the combination of visible and invisible rays which it supplies.

Besides being a highly efficient source of radiant energy in the visible and adjacent invisible bands of radiation, the carbon arc is highly flexible in respect to quality of emission. This flexibility is due, in part, to the three basic types of operation to which the electric arc between carbon electrodes can be adapted:

1. Low-intensity operation between solid or "neutral-cored" electrodes.
2. Low-intensity operation between electrodes having cores containing flame-supporting materials.
3. High-intensity operation.

Distinguishing characteristics of these three types of arcs have been described in previously published articles.[1,2] Briefly, the low-intensity arc between solid or neutral-cored carbons is adapted to a relatively simple type of lamp mechanism. At high arc voltage it is a very efficient source of ultraviolet in the range from 3,700 to 4,000 angstroms. The positive crater of the d-c low-intensity arc provides a steady concentrated light of the greatest brilliancy and whiteness available from a purely incandescent source. With a peak brilliancy of 180 candles per square millimeter and an average color temperature of about 3,550 degrees Kelvin, this type of arc has been extensively used for searchlights and for motion-picture projection. It has yielded its dominant position in these fields only to the higher brilliancy, whiteness, and efficiency of the high-intensity carbon arc.

The low-intensity flame-type arc, with the greater portion of its radiation emanating from the long arc stream at a brilliancy on the order of eight candles per square millimeter, is not so well adapted as the other basic types of arcs to use with optical systems that require a light source of small area and very high brilliancy. However, due to the large area of the flame, this type of arc emits a large volume of radiation which is produced at high electrical efficiency. An important advantage of the flame arc is the wide range in quality of radiation that can be obtained by varying the composition of the core[3] and the uniformity in both quality and intensity of radiation obtained from a particular type of carbon, when operated at specified arc conditions. When cored with certain rare earth compounds, the flame arc produces radiation so similar to that of natural sunlight in spectral composition that for many applications its effects are practically identical with those of sunlight. This light is the closest approach to natural sunlight of any artificial source having sufficient intensity for numerous industrial applications. Other combinations of core materials provide high emission of infrared or ultraviolet and permit emphasis on specific bands of ultraviolet, corresponding to the wave lengths at which most rapid reaction is obtained in certain photochemical and irradiation processes.

The high-intensity carbon arc is characterized by its very high crater brilliancy, 350 to 1,200 candles per square millimeter, and—with the core composition used in all but certain high-intensity therapeutic carbons—by the even distribution of all colors in its spectrum. The crater brilliancy of 1,200 candles per square millimeter is obtained from a new positive carbon only recently made available for use. A brilliancy of 2,000 candles per square millimeter has been attained with an experimental type of carbon not yet available for commercial application. The appearance of the d-c high-intensity arc is illustrated in Figure 1. Color temperature of the light from high-intensity arcs used in motion-picture projection averages about 5,800 degrees Kelvin. Maintenance of the high-intensity effect is dependent on the position of the electrodes in relation to each other. This relationship is maintained by continuous and accurately controlled feeding of the electrodes, and, in some of the larger high-intensity arc lamps, the positive carbon

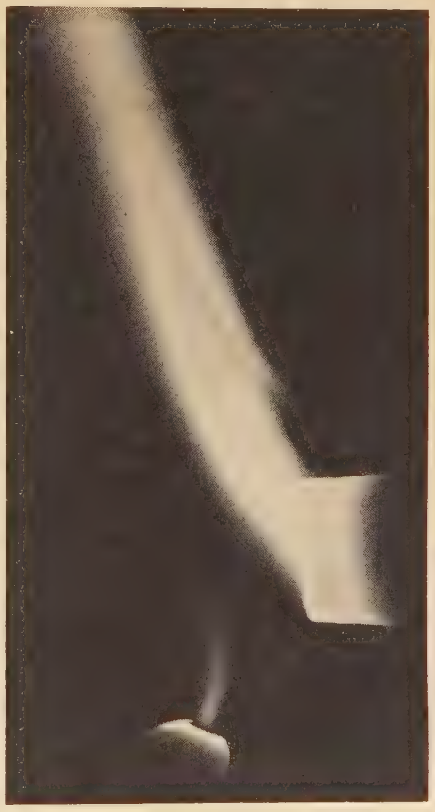

Figure 1. High-intensity carbon arc with rotating positive electrode

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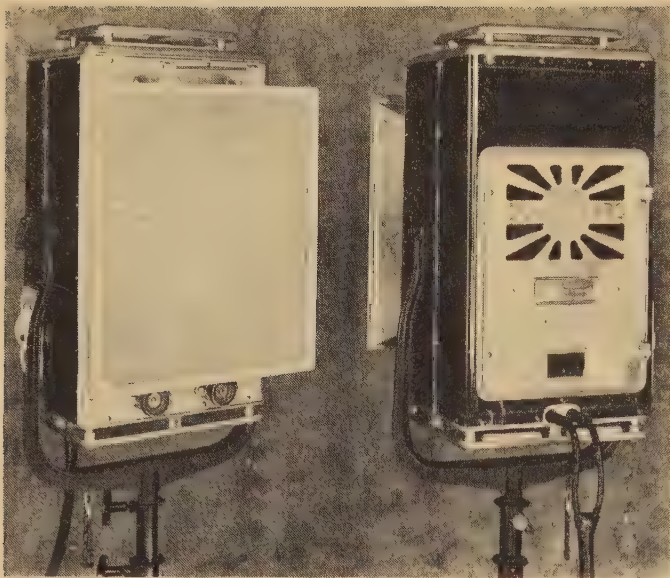

Figure 2. Twin-arc, motor - controlled lamp used for broadside lighting

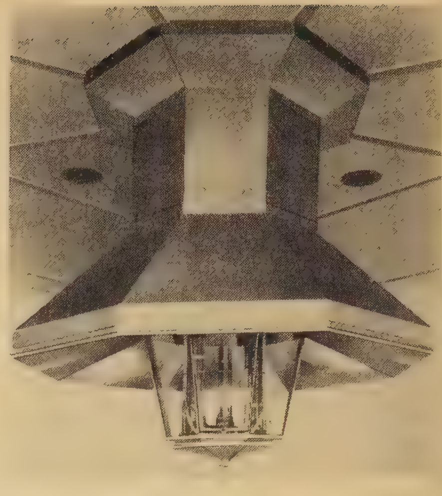

Figure 5. Four-arc carbon-arc solarium unit

is rotated, in order to maintain a symmetrical crater formation. The principal use of this highly concentrated source of illumination is for searchlights and for motion-picture production and projection.

Summarizing the advantages which give the carbon arc an important place in industry, the following points may be noted as available in one or more of the basic types:

1. Very high output of radiant energy from a single source.

2. Flexible quality of energy emission.

3. Close approximation to natural sunlight.

4. Uniform quality and intensity of emission at specified arc conditions.

5. Output unaffected by age or period of operation.

6. High intrinsic brilliancy, adapted to optical systems requiring a highly concentrated source of radiation.

7. Shape of light source and distribution of brilliancy adapted to simple optical systems.

8. High efficiency in production of both ultraviolet and visible radiation.

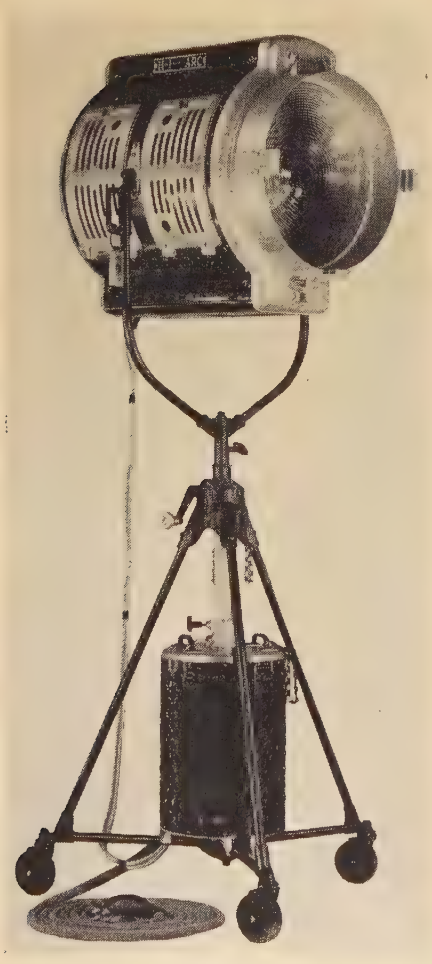

Figure 3. High-intensity carbon-arc lamp with Fresnel-type lens

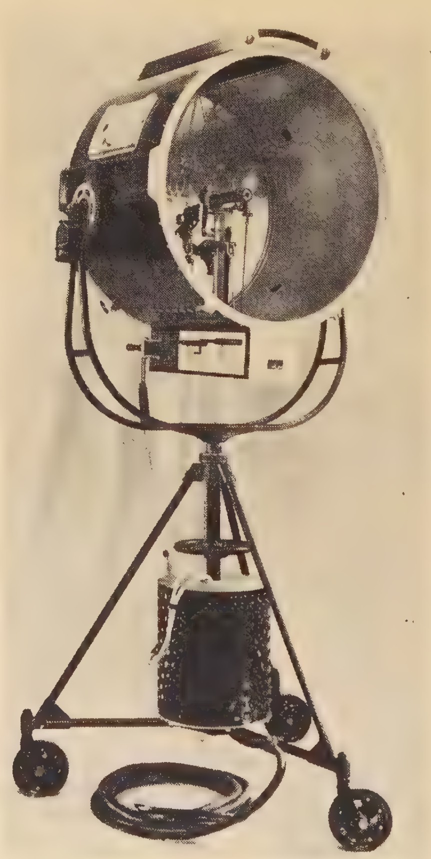

Figure 4. 36-inch sun arc

The carbon arc was adopted at an early date as an artificial source of illumination for photography and allied photochemical industrial processes, such as blueprinting, photoengraving and photolithography. High actinic power, color quality, and adaptability to high illumination intensities are among the major reasons carbon-arc lighting was adopted by these industries and are still major factors in the extension of its use. Early photographic emulsions, sensitive principally to blue, violet, and ultraviolet radiation, react rapidly to the light of the low-intensity arc. This was the only type of arc available when photographers first turned to artificial lighting. As the sensitivity of photographic emulsions was extended through green to yellow and orange wave lengths, the white-flame arc, by that time available, was adopted, because its higher output of visible radiation gave greater speed and better photographic quality than the neutral-cored carbon arc. With modern photographic emulsions, sensitive to all colors of the spectrum, the advantages of the even distribution of color in the light from the white-flame arc have been further emphasized.

The high photographic speed of the white-flame carbon arc and its similarity to daylight made it the preferred artificial light source for photoengraving and photolithography, even when production in these industries was predominantly monochromatic. The growing popularity of natural-color reproduction in the graphic arts emphasizes the value of a light which provides all of the spectral

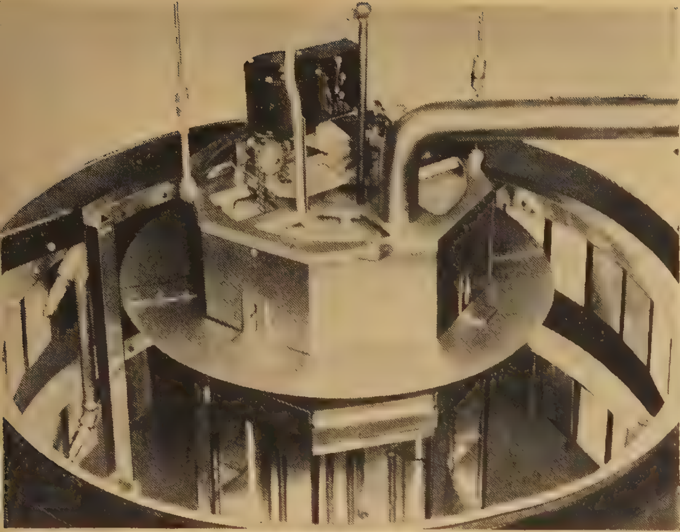

Figure 6. Interior of carbon-arc accelerated-weathering unit

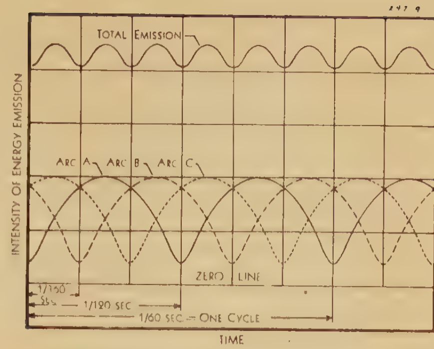

Figure 9. Energy emission from three-phase 60-cycle flame arc

colors at essentially equal intensity. High-intensity arcs are used in photolithography for large-sized reproduction by the projection method.

The motion-picture studios depend chiefly on carbon-arc lighting for natural-color photography. This also is due to the need for an even balance of the primary colors. The color balance of the light from carbon arcs, as adapted to studio use, conforms so closely to that of sunlight that the two are considered essentially equivalent for photographic purposes. The type of lamp generally used for broadside lighting is shown in Figure 2. This lamp has a motor-driven feeding mechanism which maintains very steady light output and is extremely quiet, permitting operation in proximity to the microphone without difficulty. It uses a special white-flame carbon producing light with a color temperature of 4,650 degrees Kelvin almost perfectly balanced to the color sensitivity of motion-picture film, and requiring no color-correcting filter.

High-intensity arc lamps equipped with a Fresnel-type lens, as illustrated in Figure 3, are used for back lighting, cross lighting and key lighting and for both wide and narrow beam front and effect lighting. High-intensity "sun arcs," with 24-inch and 36-inch mirrors, Figure 4, are used for extremely long throws, for sharply outlined shadows, or to provide a clearly defined beam of light through the general illumination. Because of their ready adaptability to the needs of the studio and to modern photographic technique, many cinematographers are using carbon arcs as the principal source of illumination for monochromatic as well as for color productions.

For background projection or process photography a light source of tremendous power is required to project the background scene through the screen at an intensity comparable to that of the lighting on the set. Only the high-intensity carbon arc has sufficient power for this purpose. In fact, three background projectors are sometimes used, synchronized with each other and with the camera, each projecting the same scene in exact register through the screen. The precision of mechanism and optical elements which has made possible this intricate projection system is a major engineering accomplishment.

The high-intensity carbon arc is an ideal light source for searchlights, because its tremendous output is concentrated in a very small area. This permits the light

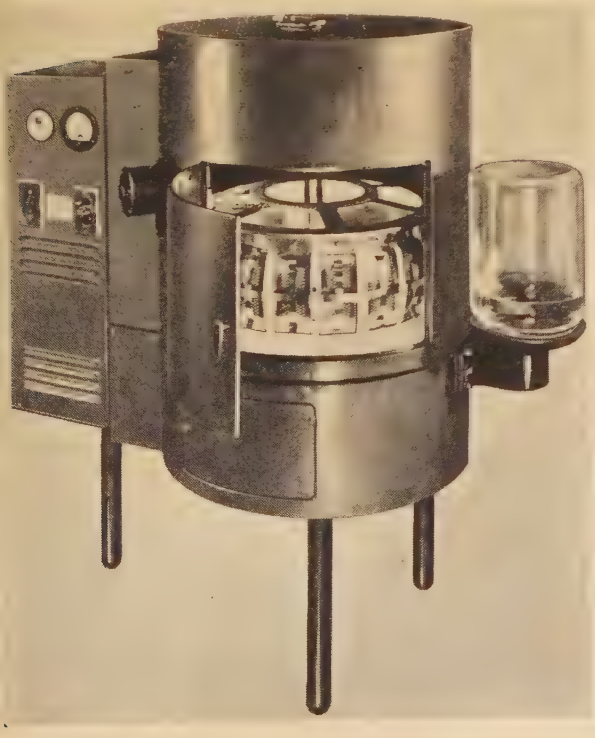

Figure 7 (left). Carbon-arc accelerated-fading unit

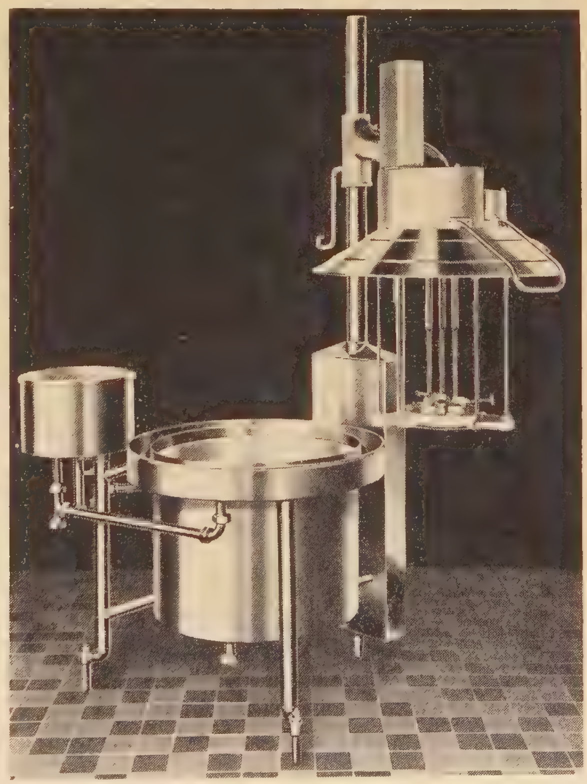

Figure 8 (right). Three-phase carbon-arc milk-irradiating unit—open

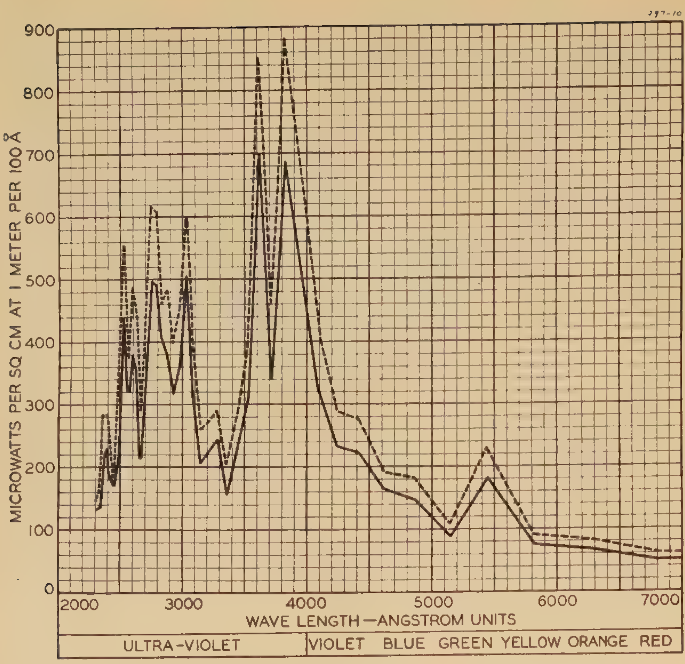

Figure 10. Distribution of radiant energy from flame arc used in milk irradiation

Dotted curve—At 80 amperes, 60 volts, a-c
Solid curve—At 80 amperes, 50 volts, a-c

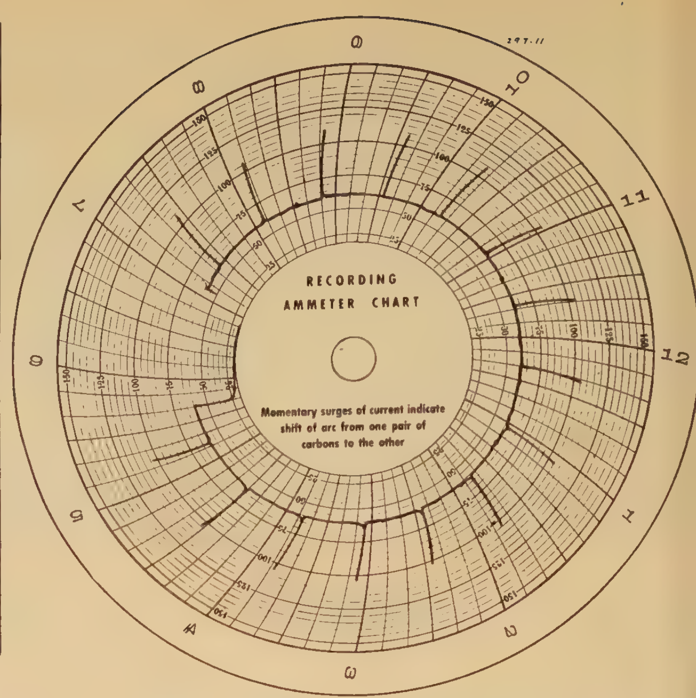

Figure 11. Recording-ammeter chart from unit shown in Figure 6

to be focused by the lens or reflector of the searchlight into a beam of small divergence and high penetration. The high-intensity searchlight with 16-millimeter positive carbon, operated at 150 amperes and 78 volts, and using a 36-inch reflector, projects a beam of considerably more than 100-million beam candle power and, with a 60-inch reflector, the beam candle power is well over 500 million. At 195 amperes and 84 arc volts the beam candle power is 30 to 50 per cent higher than at 150 amperes. Exact candle power of the beam is dependent on shadow losses, glass absorption, and the optical characteristics and reflection factor of the mirror.

Carbon arcs were used for motion-picture projection from the earliest days of the industry and are in practically universal use for theater projection today. No other commercially available light source provides light of suitable power, concentration, and color quality to meet the projection requirements of motion-picture theaters. The present trend is toward replacement of low-intensity projection lamps with high-intensity lamps of simplified design. This trend results from increasing recognition of the need for higher levels of illumination and also from popular demand for fidelity of color reproduction in the presentation of natural color features. Approximately 50 per cent of the light from the low-intensity carbon arc is in the orange and red portion of the spectrum. This color composition overemphasizes the red components in color pictures. Theatrical film of this type is processed for projection with light having approximately equal intensities of the three primary colors, a condition which the high-intensity arc fulfills most acceptably. The development of high-intensity projection lamps which require little more than one kilowatt at the arc has put the cost of high-intensity projection light essentially on the same level as that of low-intensity light, and has made it available to theaters which could not previously afford to operate high-intensity lamps. Other applications of the carbon arc for projection purposes include stereopticon projectors, spotlights, and theatrical "effect" lighting.

Since the discovery by Finsen that radiation from the carbon arc provides a cure for certain physical disorders, carbon-arc lamps have been extensively used in light therapy. The flame-type carbon arc is at present preferred for this purpose, because of its versatility in respect to quality of radiation. With one type of carbon close simulation of the physiological effects of sunlight is obtained. Other types give powerful emission in specific bands of ultraviolet. Still another type has mild ultraviolet emission, combined with a high output of penetrating radiation of longer wave length. All of these carbons can be burned in the same lamp. A large carbon-arc solarium unit, such as that illustrated in Figure 5, provides radiation of suitable intensity for light therapy over a circle of 20 standard solarium cots.

The increasing importance of quickly determining the resistance of various materials to deterioration under the action of sunlight has resulted in the almost universal adoption of the carbon arc as the source of radiation in accelerated testing units. This, again, is due to the fact that the quality of radiation delivered and the resulting effects are comparable to those of natural sunlight. Furthermore, radiation can be provided at an intensity which greatly accelerates the rate of deterioration and, being available 24 hours a day at constant intensity every day of the year, allows conclusive evidence of product quality to be obtained in much less time than is possible by means of outdoor tests.

In accelerated weathering units, such as that illustrated in Figure 6, test samples are carried on a rack which revolves slowly around the arc. At a portion of their revolution the samples are exposed to a water spray. The weathering action of water, air, thermal shock, and radiation of sunlight quality are thus combined in one test, the conditions of which can be exactly reproduced at any time and in any location. The unit illustrated employs a 60-ampere, 50-volt single-phase, a-c flame arc operated through a transformer from a 230-volt line. There are two upper and two lower carbons, but the arc burns between only one pair at a time. This unit accommodates 64 samples at one time. Paints, lacquers, enamels, varnishes, plastics, rubber and rubberlike materials, roofing and building materials, materials for leggings, tents, and tarpaulins, and special types of glass are included in the list of products for which accelerated test-

ing is now a standardized procedure. Comparative records made by users of these units indicate that an exposure of 30 hours in the unit described is approximately equivalent to one month of outdoor exposure.

Units designed for comparing the color fastness of dyes, dyed fabrics, paper, plastics, and other products do not have the water spray that is provided in the weathering unit but usually have provision for maintaining an adjustable degree of humidity. The unit shown in Figure 7 has a 30-ampere, 40-volt, single-phase arc lamp, operated through a transformer from a 230-volt line. This lamp has three upper and three lower carbons, but the arc burns between only one pair at a time. This allows it to operate continuously for 24 hours without retrimming. The revolving rack carries 18 sample holders, curved to the mean radial distance of $10^1/_4$ inches from the arc.

Recognition of the dietary importance of vitamin D has led to extensive use of carbon-arc lamps as a source of ultraviolet for increasing the vitamin-D potency of milk. Several types of carbon-arc milk irradiators are in use, one of the most efficient of which employs a 12- to 14.4-kw, three-phase, carbon-arc lamp with 80 amperes and 50 to 60 volts at each arc. This unit is shown in open position in Figure 8. In operating position the lamp is centrally located within the large drum. Besides making available in small space a high intensity of radiant energy, the three-phase carbon arc insures a continuously high level of energy emission as illustrated in Figure 9. Ultraviolet, the activating agency, does not penetrate milk to appreciable depth, and portions of the rapidly flowing film of milk in an irradiator may reach the exposed surface only during low-emission portions of the energy cycle from a single-phase source. Experience has demonstrated that the effectiveness of the irradiation process is increased by maintaining a continuously high level of energy emission. Since the flame-type arc operates more efficiently on alternating than on direct current, due to the use of reactance instead of resistance ballast, the polyphase arc is the most practicable means of maintaining a high-emission level.

Some objection to the irradiation process for increasing vitamin-D potency of milk has resulted from the use of sources of ultraviolet which produce considerable ozone and result in impaired flavor. The carbon arc is free from this objection. Coltman and MacPherson[4] have shown, by a method of spectroscopic analysis capable of clearly detecting 14 parts per hundred million, that no ozone above that concentration is present in the gases surrounding the carbon arc. Furthermore, producers of both fluid and evaporated milk have found that a vitamin-D potency of 400 USP units per quart can be obtained by carbon-arc irradiation without impairing the natural flavor of the milk.

Figure 10 shows the distribution of radiant energy from the type of flame carbon found most effective for milk irradiation. With these carbons the unit shown in Figure 8 is capable of activating 8,000 to 10,000 pounds of fluid milk per hour to a potency of 400 USP units per quart.

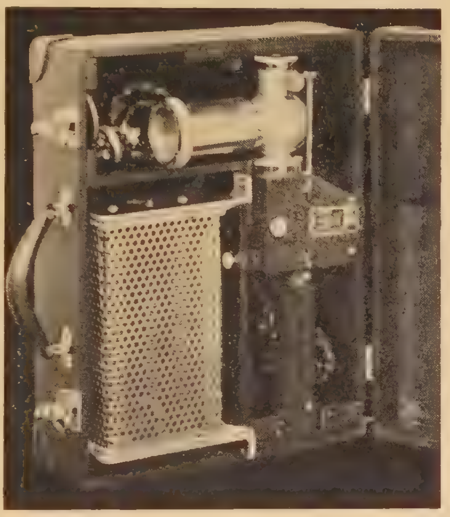

Figure 12. Portable ultraviolet carbon-arc lamp in carrying case

Uniformity of radiation from the large carbon-arc lamps used in the industrial testing, and irradiating units here described is obtained by a motor-driven carbon feeding mechanism which provides automatic control of arc current. Accuracy of control has been greatly improved by the use of a 96-pole, 75-rpm reversible synchronous motor to drive the feeding mechanism. Because of its slow speed the rotor of this motor has little momentum and does not have the tendency to overrun, after opening of the motor-control relay, which is characteristic of high-speed motors. Hunting is thereby practically eliminated. Steadiness of arc-current control realized in practice is shown by the recording ammeter chart from an accelerated weathering unit reproduced in Figure 11. The momentary peaks at approximately 40-minute intervals indicate the shift of the arc from one pair of electrodes of the dual carbon trim to the other.

The applications of the carbon arc which have been cited are representative of the more extensive uses of this versatile source of radiation for industrial purposes. Carbon-arc lamps are used also to provide light of daylight quality, closely controlled in color composition and intensity, for critical matching of color. Ultraviolet radiation from carbon arcs is used in the processing of tobacco and other commercial products and in the preparation of certain pharmaceutical materials. Spectroscopic carbon and graphite electrodes of very high purity are used for spectroscopic analysis and as a source of radiation of reproducible intensity and spectral energy distribution for high temperature calibration and for color standardization.[5] The ash content of these special electrodes is considerably below 0.001 per cent. A portable carbon-arc lamp fitted with ultraviolet filter, Figure 12, is used in both laboratory and field for fluorescence inspection and analysis. Many of these lamps are used in crime detection. Other important industrial applications of the carbon arc, entirely outside the field of illumination, are in electric welding and in electric arc furnaces used for a variety of electrochemical and electrometallurgical processes. Although no longer before the eye of the general public, as when carbon-arc lamps hung on every street corner, this source of radiant energy is today more widely used and of greater industrial value than at any time in the past, and further extension of its commercial and industrial applications may confidently be expected.

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A New Moving-Magnet Instrument for Direct Current

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Synopsis: A new type of d-c instrument is described in this paper. It comprises a diametrically magnetized cylindrical moving-magnet element, a hollow copper damping cylinder surrounding the moving element, a fixed coil, control magnets having a very high coercive force, and an enclosing magnetic shield made of mumetal. The field of the control magnet and the field of the fixed coil have a relatively large angular displacement. The angular position of the vector sum of these fields varies with the current through the coil, and the moving element follows this vector. The scale distribution is nearly uniform for a 90-degree scale. The characteristics compare favorably in many respects with those of a D'Arsonval instrument.

OERSTED'S discovery that a compass needle tends to set itself at right angles to a wire carrying an electric current provided the basis for the design of the first electromagnetic indicating instrument, which consisted of a coil of wire surrounding a magnetized needle.[1] This in turn led to the development of the astatic galvanometers of Nobili and Kelvin and of the sine and tangent forms of galvanometer. These latter instruments, which depended upon the earth's field for their control torques, were obviously unsuited for use as portable instruments.

Ayrton and Perry, in designing the first portable d-c ammeter, used a large permanent magnet to produce a controlling field. In this instrument the polarization of the vane was derived from the fixed permanent magnet rather than from its own permanent magnetism. Since the field of the control magnet had to be so strong that the earth's field was negligible by comparison, the field of the coil had to be correspondingly strong in order to deflect the vane. This made the sensitivity of the instrument inherently low, since so many ampere turns were required to excite the field. Modifications of the Ayrton–Perry instrument are used for automobile ammeters, and similar applications where low cost and ruggedness are important, and where sensitivity and high accuracy are not required.

The principle of the permanent-magnet moving-coil galvanometer as developed by Sturgeon, Kelvin, and D'Arsonval was used by Weston in the design of the first moving-coil portable instrument. This principle is now generally used in all d-c instruments of high accuracy and high sensitivity.

Development of New Moving-Magnet Design

Although highly sensitive suspended magnet instruments have been built, no portable moving-magnet instruments have come into general use. "Moving-magnet instrument" is here intended to denote an instrument in which the magnetomotive force of the moving element is much greater than that produced by the fixed coil or by the fixed control magnets.

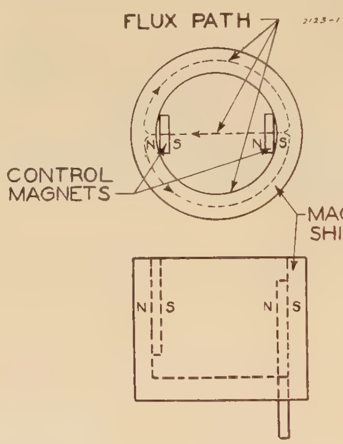

Figure 1. Path of control-magnet flux in magnetic shield

In an instrument of high sensitivity, the field of the fixed coil will be rather weak because of the small power available for producing it. This means that the instrument must be well shielded against stray fields which, even in the case of the earth's field, would be comparable in strength with the field produced by the instrument coil. This is accomplished by enclosing the moving magnet, coil, and control magnets in a cylindrical cup of magnetic material of high permeability and low coercive force. Permalloy or mumetal[2] are suitable materials for this shield.

Controlling torque may be obtained either from a magnet or from a spring. In this instrument a magnet was preferred because of ease of adjustment and other considerations. Since the control magnet must be placed within the shield, it must be very short in the direction of its magnetization and must have its magnetization unaltered by the field of the moving magnet. It is apparent that a material of very high coercive force is required. This requirement is met by an alloy of silver, aluminum, and manganese which was first described by Potter.[3,4] This alloy has a coercive force ($_{I}H_{c}$) of 6,000 oersteds and a residual induction of 610 gausses. It is unique among permanent-magnet materials in having a coercive force of this high value. The high coercive force enables the control magnets, which are in the form of thin strips, to be magnetized in the direction of their thickness. As shown in Figure 1, these magnets are mounted on opposite ends of a diameter inside the cup so as to produce a control field in the direction of this diameter. The strength of this field may be decreased or increased by inserting or withdrawing one magnet through a hole in the bottom of the shield as shown in the right-hand magnet in Figure 1. This adjusting motion is parallel to the axis of the cylinder and does not affect the direction of the control field in the plane in which the moving magnet turns. Thus, it controls the sensitivity without affecting the zero setting or the scale distribution of the instrument.

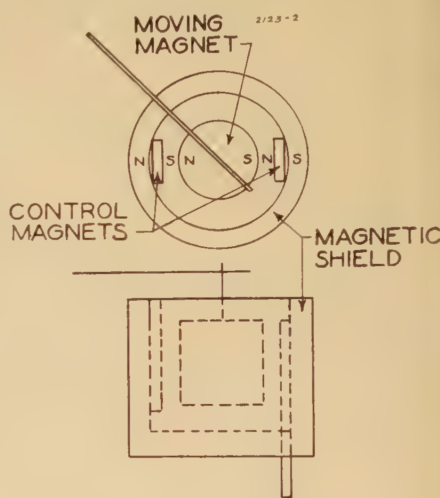

Figure 2. Relative positions of moving magnet, control magnets, magnetic shield, and pointer

The moving magnet should have a small moment of inertia in order to give good responsiveness and damping. At the

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same time it should provide a strong field in order to give as high torque as possible. Since the moving element must revolve through an arc of 90 degrees or more, the largest volume of permanent-magnet material can be put into a rotor which is bounded by a surface of revolution. From these considerations it appears that a cylindrical shape will be suitable for the moving magnet. A cylindrical magnet, magnetized along its diameter, has a very small ratio of length to cross-sectional area and, hence, should have a much larger ratio of coercive force to residual induction than that of any of the commonly known magnet steels. The silver alloy used in the control magnets does not have a sufficiently high residual induction for this application. A very good material is an oxide magnet made of a mixture of iron and cobalt oxides.[5] This material has a coercive force of approximately 950 oersteds, a residual induction of 2,200 gausses, and a $B \times H$ maximum value of

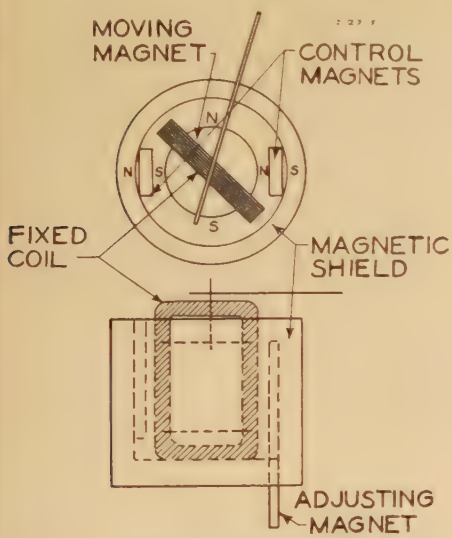

Figure 3. Diagram of complete moving-magnet-instrument element

approximately 10^6. The specific gravity of this material is half that of steel. Since the factor of merit of an electrical instrument depends on the weight of the moving element, this material is equivalent to steel having twice this $B \times H$ maximum value. Figure 2 shows the cylindrical magnet mounted within the shield and control-magnet assembly.

The field of the moving magnet has no appreciable effect on the permanent magnetization of the control magnets, because their coercive force is so high as to require a much stronger field to magnetize or demagnetize them. The field of the control magnets is much weaker than that of the moving magnet and likewise has no appreciable effect on its permanent magnetization. The shield material is of such low

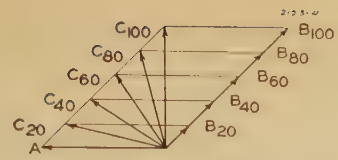

Figure 4. Vector diagrams of fluxes at intervals of 20 per cent of full-scale current

(*A*) Control magnet flux

(*B*) Coil fluxes for currents indicated by subscripts

(*C*) Resultant fluxes

coercive force as to be practically incapable of acting as a permanent magnet. It is evident that under the conditions of operation no appreciable shift in the permanent magnetization of any of the parts is possible, and hence the instrument will have negligible hysteresis errors.

The current-conducting coil is placed in the circuit to produce a magnetic flux at an angle of 135 degrees from the flux of the control magnets. This coil is shown schematically in Figure 3.

The vectorial relationship of the control-magnet flux and coil flux is shown in Figure 4. Vector B represents the coil flux. This flux is proportional to the current flowing in the coil and is shown in various magnitudes with the subscript indicating the percentage of maximum current. These B subscript vectors add to vector A to produce vector C with subscripts as shown.

The polarized rotor will turn to a position parallel to vector C subscript. It should be noted that, although vector C may vary in magnitude as well as direction, only the directional changes can cause the rotor to turn.

In Figure 5 the angular position of vector C is plotted with the per cent coil current. Since this angular position is the same as the pointer position, the plot shows the scale distribution obtained from an instrument of this type. The maximum deviation from uniformity, as shown by the straight dotted line, is five degrees.

It is at once apparent, if changes in magnitude of vector B produce directional changes in vector C, that like changes in magnitude of vector A will also produce the same effect. Use of this fact is made in calibrating the instrument to a predetermined full-scale current value. This is accomplished, as previously mentioned, by sliding the adjusting magnet of Figure 2 in or out of the metal shield cup, and thus changing the amount of control-magnet flux as represented by the magnitude of vector A.

Figure 6 is a cut-away view of one of the instrument elements.[5]

The element shown has polished steel pivots moving in brass bearings. This type of bearing can be used where torque-to-weight ratios are sufficiently high.

The copper damping cylinder is used to bring the rotor to rest rapidly, by means of eddy currents induced by the rotor.

The field coil is wound at an angle across the molded coil form and not parallel to the rotor axis, as shown schematically in Figure 4. This was done for mechanical reasons and in no way changes the previously discussed considerations, since the field of the coil has a component in the plane of vector B.

Zero adjustment is accomplished by rotating the whole element assembly by means of an arm welded to the bottom of the shielding cup.

The magnetic shielding is completed by means of a mumetal washer on the top of the molded coil form.

The element as measured on the outside of the shielding cup is approximately three-fourths inch in diameter, and one-half inch long.

Conclusions

In production quantities the element has been applied to only one device. This is a dual-range storage-battery testing type of voltmeter shown in Figure 7. Here it was possible to use brass bearings. In all applications seen by the authors of moving-coil permanent-magnet instru-

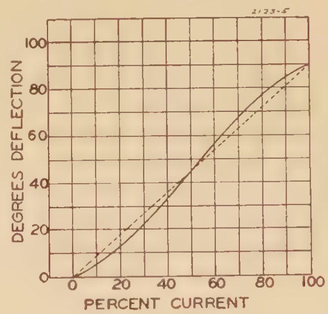

Figure 5. Scale-distribution curve for moving-magnet instrument

ments to similar devices, jewelled bearings are used.

Table I shows the physical constants of these voltmeters.

The elements are adjusted to fit predetermined scales to an accuracy of ±2 per cent of full-scale reading.

No hysteresis error (measured by taking calibration at the same point with descending and ascending currents) can be detected.

The instrument, when placed in a stray field of ten gausses in such a manner as to produce maximum errors, changes read-

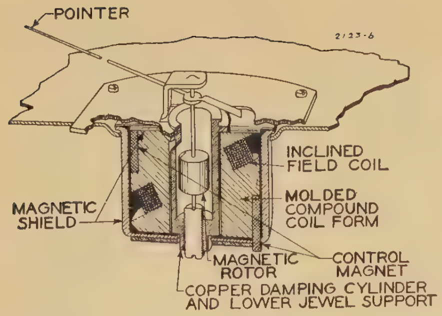

Figure 6. Cross section of moving-magnet instrument

Figure 7. Battery-testing voltmeter with moving-magnet element

Table I

1. Scale angle	90 degrees
2. Scale radius	$1^{15}/_{32}$ inches
3. Scale length	$2^{5}/_{16}$ inches
4. Full-scale torque (T)*	0.35 millimeter-gram
5. Weight (W)	0.205 gram
6. Ratio T^*/W	1.7
7. Ratio T^*/W 1.5	3.9
8. Response	1.5 seconds
9. Rating	3/10 volts
10. Ohms per volt	20
11. Coil resistance	20 ohms
12. Coil watts (full-scale)	0.05
13. Coil ampere-turns (full-scale)	14

*The full-scale torque shown (T) should be multiplied by approximately 3/2 to be comparable to the conventional 90-degree torque of moving-coil instruments. This is for the reason that the torque-versus-angular-displacement curve of the moving-magnet instrument is roughly sinusoidal and not linear as in the case of the moving-coil instrument. The 3/2 figure comes from extending the slope of the torque-displacement curve from near the origin, to the 90-degree point. This is logical, since it is the increment of torque-near-zero displacement which determines the inherent friction "stickiness" of the indicator.

ings approximately one-half of one per cent of full-scale reading.

The temperature errors of these indicators show some peculiar characteristics because of the high magnetic temperature coefficient of the silver alloy control magnets. With 10 per cent copper in the circuit, the element shows an error of −4 per cent at −40 degrees centigrade and +2 per cent at +50 degrees centigrade. With 35 per cent copper the error at −40 degrees centigrade is +1 per cent, and at +50 degrees centigrade approximately 0 per cent. The per cent errors are deviations from readings taken at 20 degrees centigrade. This makes it possible to build a voltmeter of low temperature coefficient with a high ratio of coil to series resistor resistance. Temperature compensation of ammeters may be obtained by a shunt resistor having a low temperature coefficient of resistance.

The element as used in these battery testers withstood considerable abuse. Several were tested by dropping 30 inches onto hard wood and concrete floors. No changes were noted except in one case where the adjusting magnet came loose.

The authors feel the element has inherently lower cost features and will withstand more abuse than the conventional moving-coil permanent-magnet element. By extending the design to the use of jewels, the sensitivity can be increased. Although it cannot reach the higher sensitivities of moving-coil permanent-magnet design, this moving-magnet element can be produced in sensitivities high enough to fit many applications now using the latter design.

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Sleet Problems on Electrified Railroads

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Synopsis: Sleet storms, or more properly ice storms, have always presented serious difficulties to the operators of overhead electric conductors of all classes. For more than 25 years some electric-power companies have been using the circulation of electric current to heat the conductors, either to melt off the ice or to prevent it from forming. The technique of such procedure is well-known and will not be dealt with in this paper, other than to present fundamental theoretical data.

Electrified railroads operating with overhead contact systems have similar ice storm problems, and, in addition, a number of special problems caused by such ice. These problems are outlined in this paper, and some means which have been used or proposed for their solution are described. Operating experience with one special application to a case of severe exposure to ice storms and high wind velocities is cited. Some of these special railroad problems have not yet been satisfactorily solved.

General Problems

THE accumulation of ice caused by freezing rain, also known as sleet or glaze, on overhead electric conductors has always presented problems to be dealt with by operating engineers as well as by the designing engineers of such systems. Ice loads must be taken into account in calculating the strength of the conductors and their supporting structures, and the National Electrical Safety Code[5] has set forth the different values of radial thickness of ice which may be expected to occur in the various parts of the United States. However, even the most liberal provisions for ice loads are exceeded during the occasional unusually severe ice storms, and the conductors and even their supporting structures may then fail.

Ice storms are frequently followed by a drop in temperature accompanied by high-velocity winds, and both the designers and the operators are then faced with additional problems, for to the static ice loads may be added dynamic forces caused by swaying loads, and under certain critical wind velocities ice-covered conductors will whip up and down and "dance" violently even to the extent of destroying themselves or their attachments to their supporting structures.

To mitigate all these effects, which cannot always be provided for economically in the design, ice melting by heating the conductors with electric current has been resorted to by many power companies. This is done by loading the lines to capacity where possible, by load concentration or exchange with connected supply lines, or by the circulation of sufficient electric current at a voltage lower than normal; and routine procedures have been established during such storms to remove or to prevent the forming of ice on the conductors by these means.

The theory of such current requirements and the various operating techniques used have been reported in the technical press and in papers before operator associations from time to time during the past few years, some of which are listed herein for reference.

Special Railroad Problems

Electrified railroads having overhead contact systems and associated power feeders must deal not only with the same general problems of this nature that electric-power-transmitting companies have, but in addition they have a number of associated problems peculiar to themselves to contend with during ice storms. Some of these are:

1. On the contact wire, the ice accumulation, in itself a poor conductor, interferes with the current collection if it becomes too thick and causes severe arcing, which can seriously damage the contact conductor by burning and pitting; and can even cause flashovers where clearances to grounded structures are limited or reduced. Frequently the collector shoe can be burned in two by such arcing before many miles have been traveled. Contact wire wear is increased by the roughened collector shoe, and the fine polish acquired by months of lubrication or otherwise, may be destroyed.

2. The static ice load on the catenary contact system may become great enough to distort the vertical alignment by greatly increasing the sag at the center of the span, causing the collector shoe to skip and arc at the suspension points, since the pantograph is usually sluggish in its action under such conditions. Where heavy ice accumulations are known to be possible, the designer should weigh this factor in the selection of span length, especially if high-speed operation is involved, since this distortion will increase directly with the square of the span length for the same weight of ice.

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With inclined catenary construction, the increase in static load may distort the horizontal alignment enough to dewire the collector shoe on long spans on curves of long radius. Here again the remedy lies with the designer by using shorter spans or applying adequate horizontal bracing.

3. The additional wind load on the ice-covered contact system may result in further alignment distortion caused by horizontal sway, or in vertical "dancing," which, even if not severe enough to damage the overhead system, is in itself detrimental to continuous current collection and to high-speed operation and continuous tractive effort. Violent dancing may under extremely severe conditions wreck the line. Adequate bracing to take care of horizontal sway may have no beneficial effect on and may even augment this vertical dancing under critical conditions.

4. The ice accumulation on the pantographs and overhead collector shoes may add so much extra weight to these parts as to completely nullify the normal spring tension which holds the shoe against the contact wire. Then the pantograph will drop away from the wire or will not rise again after having been depressed, where the contact wire is lower than normal, as under a highway bridge or other low overhead structure. A means of augmenting the normal spring pressure, by added air pressure or otherwise, is one means of overcoming this difficulty; but it must be remembered that the pantograph pressure cannot be made too great without affecting the contact system.

5. Electric melting of ice on the overhead contact system may require larger currents than are available, since such systems are, in general, designed with more conductivity and for heavier currents than in the average power-transmission lines, which are usually of much higher voltage. Further, even when large currents can be applied, they may concentrate in the messenger or in some associated wire other than the contact wire, as some contact systems may be designed to have most of the conductivity in some member other than the wearing wire. In general, it is desirable to have the heating take place in all the members of the contact system, to overcome the difficulties outlined in paragraph 2 above as well as those in paragraph 1.

6. Unlike ice-melting schemes used on power-transmission lines, any electric ice-melting scheme used on an overhead contact system must allow of being superimposed on the contact system circuit while it is normally energized with traction power for train operation; that is, the heating current must be of such characteristics that it will not interfere with the electric operation of trains supplied with power from that same wire or circuit. For example, it is obvious that direct current cannot safely be superimposed on a-c circuits without the danger of saturating the a-c equipment and rendering it inoperative or possibly damaging it by overheating. If it becomes necessary to remove the traction power to apply a heating current of different voltage or characteristic —as may be done often on power transmission lines—there would be no advantage of such a method over the suspension of elec-

tric operation entirely and the substitution of steam or other form of motive power for the duration of the storm—an extremely inefficient, if not impossible procedure on a system having dense traffic.

Within the past two or three years, several unusually severe ice storms in the northeastern part of the United States have caused all of the electrified railroads in this section which operate under an overhead contact system to give more attention to these special problems, with the objective of preventing the ice from forming on the contact wire or collector parts, or of removing the accumulated ice before the deposit becomes unduly troublesome.

This paper will discuss some of the various solutions which have been found or suggested for these special railroad problems, rather than those which have generally been applied to power transmission lines. Some of the fundamental data are applicable to these special problems, as well as the general problems, and hence are included herein for reference.

Electric Heating of Overhead Conductors

Pender's Handbook gives the following formula for the value of the current, I, required to heat a conductor:

$$I=K\sqrt{\frac{Td^3}{r}}$$

where

T is the temperature rise in degrees centigrade
d is the diameter of the conductor in inches
r is the resistance of the conductor in ohms per mil–foot at final temperature
K is a constant depending upon surface condition of wire; whose value is (approximately) 800 in still air and 1,000 in open air for solid wire; and 1,200 in open air for stranded conductors

F. R. Longley of Western Massachusetts Companies in an excellent paper on this subject presented at a symposium of System Operators of New England at Montpelier, Vt., June 25, 1937, has developed the curves shown in Figures 1 and 2, which show the amperes required to heat conductors in still air, and the cooling effects of cross winds on the temperature rise, respectively. His formula was developed from data reported by Schurig and Frick.[2] It is to be especially noted that low wind velocities cause a marked reduction in temperature rise, while wind velocities in excess of 15 or 20 miles per hour do not produce much additional reduction. It is obvious that all these formulas are applicable to railway feeders and to the various wires of the catenary contact system.

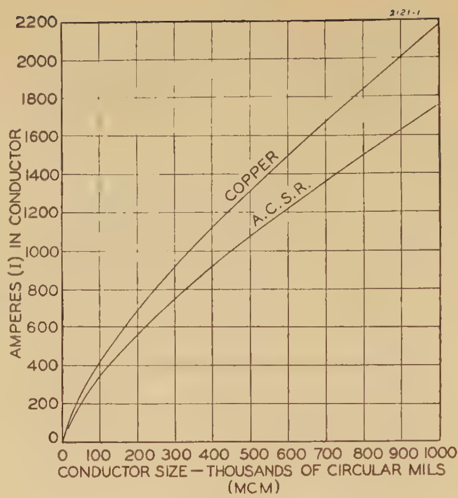

Figure 1. Current required to raise the temperature of a conductor from 0 to 150 degrees centigrade in still air (Longley)

This current is recommended for ice melting. Maximum temperature rise out of doors probably will not exceed 120 degrees centigrade, due to cooling effect of minimum crosswise wind velocity of one mile per hour. Conductors will not anneal below 175 degrees centigrade

For copper $I=17.5\ (\text{MCM})^{0.696}$

For steel-reinforced aluminum $I=14.3\ (\text{MCM})^{0.696}$

Mechanical Removal of Ice From Contact Wires

Most of the railroads which have been electrified in the northeastern part of the United States have rather frequent and dense traffic—in most cases one of the factors responsible for the electrification. Under such conditions, the usual ice storm presents no especial problem, as the ice or glaze is scraped off or shaken off from the contact wire before it has a chance to build up to a troublesome thickness, by the frequent passages of the pantographs. This is one method of ice removal which power-transmitting companies cannot use. On branch lines where traffic is light, or during the night or at other times when traffic is less frequent, if the ice formation and accumulation is mainly because of infrequent train operation, a special car or locomotive (electric) may be assigned to operate frequently to and fro over such lines to keep these lines as clear by this means as those having denser traffic.

A method of prevention of the formation of ice coatings which has been tried out on some of the 600-volt d-c urban lines, especially where trolley coaches with sliding collectors are used, and where even a light ice covering presents a very real problem to continuity of service, has been to coat the contact wire with a waxy or greasy substance which will not become wet enough by light rain to allow the formation of a continuous film but will cause the water to collect in drops. This may be partially successful in light ice storms of short duration, but since the drops which adhere and freeze have the same surface tension as the rain drops, they finally unite to form a continuous coating. Very possibly the waxy coating causes the ice coating to be less adherent, but when the temperature drops, it remains on the wire until scraped off by the collector shoe. This measure obviously is ineffective for very severe storms and is not economically applicable to the contact systems of electrified railroads.

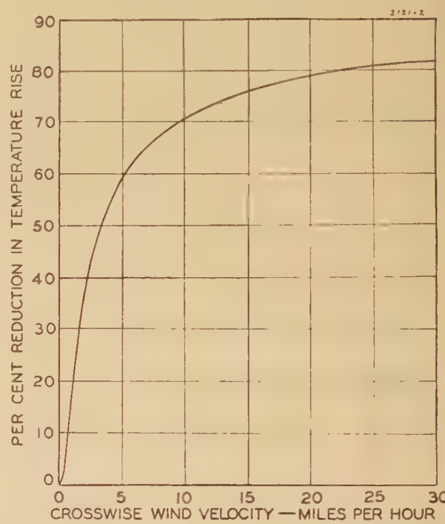

Figure 2. Cooling effect of crosswise wind on heated conductor (Longley)

Special Cases and Their Treatment—an Example

Certain sections of railway line may be exposed to unusual conditions favorable to the frequent and very rapid formation and accumulation of ice and to very high winds. On such sections even frequent traffic is not in itself sufficient to keep the contact wires clear of ice, and some other method of ice removal must be employed if service is to be maintained.

An example of such a section, having unusually severe exposure to both heavy and rapid accumulations of ice and to very high wind velocities, is that part of the electrified system of the New Haven Railroad known as the New York Connecting Railroad which operates over the Hell Gate Bridge in New York City between the Bronx and Long Island. For a distance of nearly four miles this part of the

railroad is on a viaduct which is from 75 to 165 feet above the ground or river level and at a considerable distance from any shielding structures. Ice seems to form very rapidly under certain conditions in this section and at times when it is not troublesome elsewhere, and the wind velocities in winter, especially after such storms, assume unusually high values. Consequently, this part of the overhead line has been damaged by ice and wind to the extent of being rendered inoperative electrically on several occasions since its construction in 1917. In contrast to this unsatisfactory record, the remainder of the line from New York to New Haven has operated successfully through a number of severe ice storms since its completion in 1914. With the exception of one extremely severe ice storm in December 1915, when accumulations were two inches in radial thickness in many places, causing the breakage, chiefly at splices, of some number 3 American Wire Gauge copper-stranded conductors used in 300-foot spans for control circuits, this overhead system has withstood a number of severe ice and wind storms (including the hurricane of 1938) without serious damage.

Mechanical Methods Applied

A number of studies of and changes in the Hell Gate Bridge section have been made in an endeavor to overcome the difficulties with ice and wind. Very possibly a fundamental difficulty is that some of the spans used are too long for such severe exposure. In 1927 an extensive horizontal wind-bracing system was applied to the catenary system in this section. This bracing did not eliminate the vertical "dancing" of the catenary spans, and in 1933 some additional vertical stays were added to the horizontal bracing system. Obviously these measures did nothing to eliminate the ice problem. Ice on the contact wires was scraped off when possible by special "ice breaker" locomotives, operated as a routine procedure during each ice storm. The vertical dancing was still so great at times that even these locomotives were unable to keep their pantographs on the wire. The difficulties from ice and high winds continued on the feeders, which were on occasions during subsequent ice storms broken in two and torn from their supporting pin insulators in many places by the violent wind whipping. The ground wire suffered the same fate, and it is safe to say that there are but few spans in this entire section where some of these conductors have not been broken and spliced. The feeders are 4/0 American Wire Gauge hard drawn stranded copper, having a normal sag of five feet in a 300-foot span, and it is a rather awesome sight to see these heavy conductors whipping up and down in a storm for 10 or 15 minutes at a time, with a frequency of about 25 vibrations per minute and with maximum amplitudes of twice the sag at the center of the spans.

In 1937 a number of experimental changes was made to the feeders, including the installation of some experimental dampening devices similar to the Stockbridge dampeners, but of various weights and dimensions. Some experimental spring suspensions and spring dampening devices were included, and in a number of spans the feeders were removed from the pin insulators and attached to suspension insulators. None of the dampening devices proved of great value during the following winter, but the wind whipping seemed to be materially reduced on those spans where the suspension insulators had been substituted for the original pin insulators. These experiments led to the general substitution in 1941 of suspension insulators for pin insulators on all the feeders throughout this entire section, except on the very short spans in the 1,100-foot section across the main arch span of the Hell Gate Bridge, where pin insulators with a flexible mechanical clamping arrangement are used.

The ground wire also has been flexibly suspended, so that any structure vibration caused by the whipping of the catenary will not be communicated to this wire. Some additional lateral and cross bracing of the catenary contact system was also installed in 1941, and to date these changes have seemed to be effective, as no serious difficulty has been experienced this past winter, although the season has not been a severe one.

Electrical Methods Considered

During the summer of 1939 serious consideration was given to the possibility of melting sleet from the wires in this section by electric heating.

There are four tracks in this section, an eastbound and westbound track each for freight and for passenger service, not connected by crossovers except at some distance east of the section, so that this section is operated as two two-track railroads. There are four contact wires with catenary construction at 11,000 volts to rail, and four feeder circuits, also 11,000 volts to rail, but 22,000 volts to the contact circuits, thus making with the rails a three-wire system. All contact circuits and feeder circuits are sectionalized at switching stations at each end of the exposed section approximately four miles apart. All trolley contact circuits are connected to a "trolley" bus, and all feeder circuits are connected to a "feeder" bus at each station, with a 3,000-kva "balancer" transformer having center point of winding connected to rails, connected across these trolley and feeder busses at each station. The west switching station is unattended and is operated by supervisory control from the east switching station, which is located at a signal interlocking tower, thus making the east station the more desirable location for any additional switching or heating apparatus.

In order to electrically remove the ice after it had been once formed, it was assumed that the temperature of the wire would have to be raised about ten degrees Fahrenheit between the two switching stations. It was calculated that about 750 amperes would be required in the catenary system (consisting of a bronze messenger, copper auxiliary wire

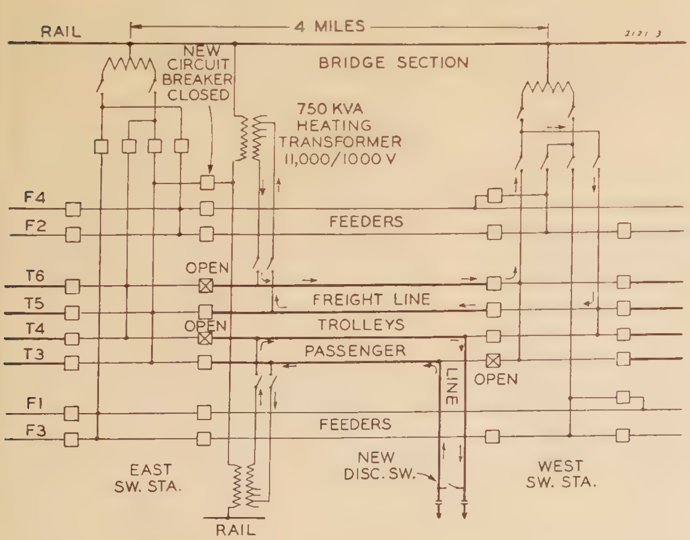

Figure 3. Scheme I proposed for heating contact wires on Hell Gate Bridge

and 4/0 bronze contact wire) to cause enough current (approximately 200 amperes) to flow in the contact wire to raise its temperature the required ten degrees Fahrenheit, since the greater part of the current would flow in the auxiliary wire and a smaller part in the messenger. The d-c resistance of this four-mile section of catenary is approximately 0.5 ohm, which gives a drop of 375 to 400 volts between stations. The a-c impedance is approximately ten per cent higher.

The problem, therefore, was to circulate in a loop circuit between sectionalizing points approximately 750 amperes at 900 to 1,000 volts a-c, or about 750 kva. A smaller amount of power and current would be required if a smaller rise in temperature would keep the lines clear when applied before and during the ice storm. The air temperature during an ice storm is always between 30 and 34 degrees Fahrenheit and if the temperature of the wire could be maintained at, say, 37 degrees Fahrenheit ice could not form on it.

Two schemes, both believed to be practical, were proposed and developed. Scheme I, shown diagrammatically in Figure 3, contemplated using two special 11,000/1,000-volt 25-cycle transformers, having their 11,000-volt winding connected to the trolley bus through a circuit breaker at the east switching station. The 1,000-volt winding would be provided with five or six taps at 50-volt intervals, with tap-changing facilities to suit temperature conditions, and would be insulated to withstand the normal contact-wire voltage plus the usual surges experienced on the system. This winding of each transformer would be connected through disconnecting switches, one to the two freight-line contact wires, and the other to the two passenger-line contact wires. These switches would be closed prior to the advent of the ice storm, and if the storm developed, the circuit breaker would be closed by the tower operator, and two of the trolley circuit breakers would be opened at the east switching station to disconnect one side of each loop circuit from the bus to prevent short-circuiting the low-voltage windings. The heat would then be on the bridge section without further switching operations, and train operation could be maintained and handled as required with no interference from the superimposed heating power. Two trains, both passenger or both freight, when passing near the easterly end of the section might temporarily bypass a small amount of this current, but it would be negligible because of the high impedance of the locomotive transformers to the 1,000-volt heating current.

The passenger line diverges from the freight line at the west switching station and continues for another mile, where it joins another system. If it were desirable to heat these circuits, a new disconnecting switch could be installed at the end of this section as shown in the diagram, closed during an ice storm, and one of the circuit breakers on this line could be opened at the west station to avoid short-circuiting this part of the line. This scheme could be expanded by additional switches, or by separate transformers to heat the feeders in the same manner, although this was not felt to be so urgent at that time.

This scheme was believed to be practicable but was not applied—partly because of the necessity for investing in the special transformers, when at that time it was unknown just how efficacious the heating current would be on the trolley circuits, but mainly because another method, involving no investment or additional equipment of any kind, was immediately available for experimental work by the simple expedient of power exchange between supply points on either ends of this section.

This second scheme is shown in Figure 4 and involved the exchange of traction power from the supply station several miles east of this section, over one circuit at a time in the section, either trolley or feeder, to the supply point several miles west of the section. At both supply points power is purchased from the same power company, but under separate contracts, because the supply at the western end is to a different railroad company over whose lines the New Haven circuits extend. This supply is through a 7,200-kva motor-generator set, designed for power flow in either direction, which was ideal for the purpose in mind.

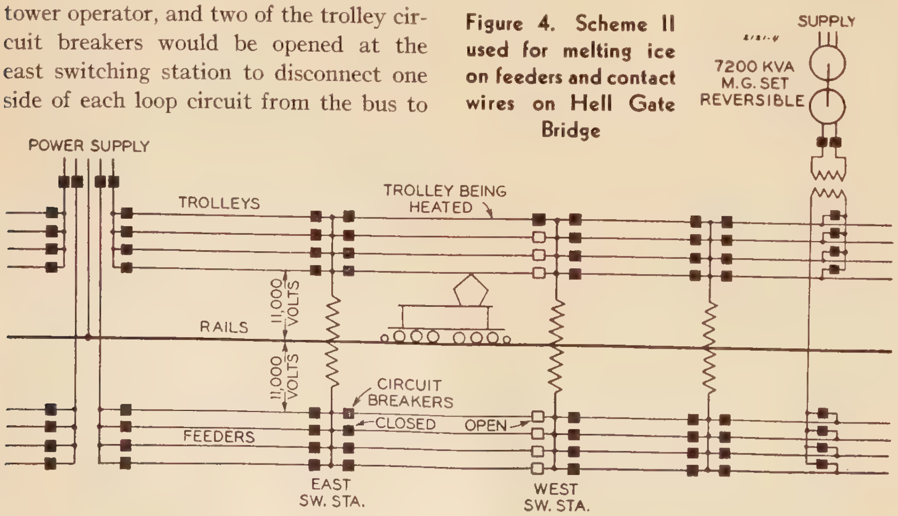

Figure 4. Scheme II used for melting ice on feeders and contact wires on Hell Gate Bridge

It will be seen that scheme II involved the use of a single trolley-rail circuit for contact-wire heating, or a feeder-rail circuit for feeder heating, in the bridge section, which necessitated some experimental tests to satisfy the local communication interests that no inductive disturbances serious enough to cause any interference with their plant could be detected; and after some further tests to develop operating technique, this scheme was adopted as a regular operating procedure to be used throughout the duration of ice storms and has been in successful operation for three seasons. This scheme has been more fully described elsewhere.[4] One of the features of the power exchange is that it is done on a half-hour basis; that is, power is taken from one supply for one half-hour and delivered (less losses) to the other supply, after which interval the power flow is reversed for the next half-hour. This is because both contracts have the maximum demand on an hourly basis, and in this way neither contract demand can be exceeded by the power exchanged. A typical power curve illustrating this feature is shown in Figure 5.

With scheme II ice already formed can be melted from the feeders in from six to ten minutes and from the catenary in about twice this time, depending upon the temperature and wind conditions and the amount of power exchanged, which is limited by the capacity of the motor generator set at the western supply.

With the improvements made during the past year in the feeder system in this section, there is some question as to the value of ice removal from the feeders, if they no longer dance and whip so violently under storm conditions. The writer has personally observed dancing or whipping of feeders in this section, when formerly supported on pin insulators, with no ice coating whatever, and at temperatures above freezing, so that there is some question as to whether such dancing is always a function of ice coating; and if not, the solution of the problem of "dancing conductors" may be mechanical rather than (or as well as) thermal. More winter experience with the changes re-

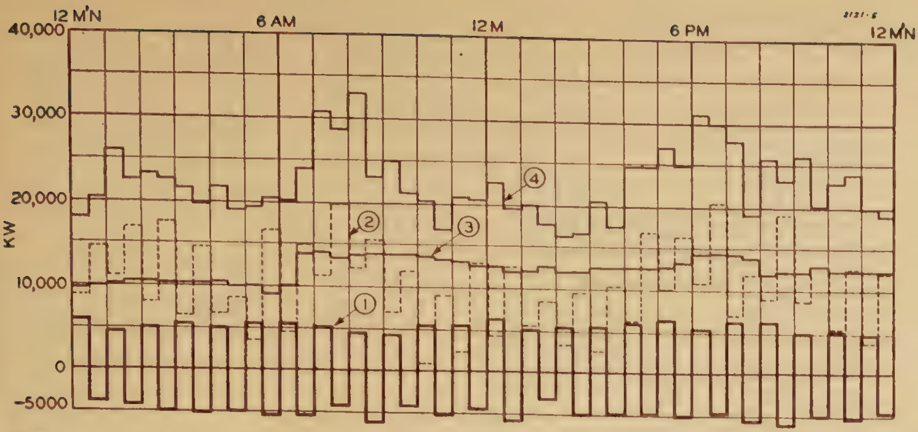

Figure 5. Power-load curve for March 4, 1940

①—Ice-melting power exchanged with west supply

②—Total purchased power, excluding ice-melting power from west supply

③—Power produced at railroad-owned plant

④—Total railroad-system load, excluding ice-melting power exchanged

cently made must be had before it can be definitely stated that this problem has been solved.

Further Considerations

It will be noted that scheme II involves the continuity of service of all equipment involved in the power exchange; and if the equipment at the western supply point, which is not in duplicate, should be out of service for any reason during an ice storm, the scheme could not be used. This actually did happen during a very severe ice storm early in 1941, and, consequently, later in the year the proposal was made that a scheme similar to scheme I, outlined above, be installed until it was learned that it would be impossible to obtain the necessary transformers because of the war.

A third scheme therefore, to be used only as an alternate to scheme II, has been devised and is now being installed. This scheme consists of taking one trolley or feeder circuit at a time in this section and switching the westerly end through a water rheostat, adjusted to allow the passage to ground (rail) of approximately 800 amperes of traction power supplied from the east end. Most of this power is necessarily lost, being dissipated through heat in the water rheostat; possibly five per cent is used in heating the wire. This scheme requires a number of disconnecting switches at the west switching station, a large tank of water with a continuous supply, and the necessary drains, to dissipate the large amount of waste heat, also sending operators to this unattended station and housing them there for the duration of the storm. It is expected that this installation will be ready for the next winter season.

It is obvious that a similar method might be applied to any electrified railroad, a-c or d-c, if the economics of the situation warranted.

Ice or Sleet on Pantographs and Collectors

The problem of ice accumulations on pantographs and collector shoes has been approached from a number of different angles. The usual and time-honored practice is to work the pantograph up and down by means of its own operating mechanism at frequent intervals during ice storms to jar off the ice accumulations mechanically. Obviously this means cannot be employed on long high-speed runs. It is also customary practice to use higher spring pressure during the winter, partly for the purpose of compensating for the expected additional ice load, and partly to exert more pressure on the wire for knocking off the ice on it. Mention has already been made of the possibility of augmenting the normal pressure at will by auxiliary air pressure, to overcome excessive added weight.

Another proposal is to provide steam jets on top of the locomotive to blow steam from the train-heating boiler at will over the pantograph frame in the lowered position. Obviously this scheme is not applicable to pantographs on motorcars, since they carry no steam-heating equipment. Another method which has been proposed for a-c electrified railroads is to have the pantograph frame split electrically at the base and all up through the frame, connected electrically only at the top by the contact shoe, and to circulate through this split frame a low-voltage current applied across the base from a separate special transformer, sufficient in value to raise the temperature of the entire frame and shoe. This current would be applied when necessary by the engineman. The traction power circuit would be connected to one side only of the base. Obviously, this scheme is not applicable to pantographs on equipment operated by direct current, at least without additional conversion apparatus.

Coating the frame and shoe with a waxy or greasy substance has been tried and is thought by some to be helpful in delaying the formation of adherent ice on pantographs. This measure has already been commented on in connection with the mechanical removal of ice from contact wires, and those comments apply here. It is possible that accumulated ice can be more easily jarred off the pantograph by working it up and down, when such coatings are applied, but again, this is a function of the temperature as well as the thickness of the accumulation.

Summary

Ice storms cause special problems on electrified railroads which power transmission systems do not have, most of them involving the current collection.

Ice accumulations may be removed from overhead contact systems by mechanical or electrical means.

Electrical heating of the contact system has been used satisfactorily on one a-c electrified railroad for a special case, and three methods of doing this have been devised, some of which may be applicable to other railroads.

The problems caused by high winds on ice-covered conductors are shared with other power-transmission systems and are serious ones. Changes to existing plant may help, but the possibilities of these difficulties should not be overlooked in the original design.

Ice accumulations on pantographs present special problems which are recognized and are being solved in various ways.

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Sealed-Tube Ignitron Rectifiers

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IN the past decade the advantages of the rectifier, when compared to other forms of conversion equipment, have led to the development of multianode steel tank and glass-bulb rectifiers. The desirable structural features and operating characteristics, however, have not been fully realized when pump-evacuated tanks and fragile glass-bulb rectifiers have been applied to small conversion units of low-voltage rating. Development of a means of starting or igniting[1] a cathode spot, each positive half cycle, on a pool of mercury has stimulated the design of a practicable half-wave or single-anode rectifier. The low density of ionization during the inverse cycle so reduced the shielding necessary to prevent arcback and, in consequence, the arc losses, that these rectifiers could be efficiently applied in the lower-voltage (250 volts d-c) fields.

Sealed, permanently evacuated steel ignitron tubes have been used in the control[2] of resistance welding currents for the past ten years; and in such service these electronic devices have given a degree of control, speed, and reliability not previously matched by mechanical means. Sealed, permanently evacuated steel ignitron tubes for rectifiers ranging in capacity from 75 to 400 kw (at 250 to 600 volts d-c), have been developed and placed in service, and it is the purpose of this paper to describe the design, characteristics, and performance of these rectifiers.

The Field for the Permanently Evacuated Tube-Type Rectifier

The field of application of the mercury-arc rectifier is considerably enlarged by the use of permanently evacuated tubes in circuits which have output characteristics similar to those of a d-c generator with nominal field control. Despite almost universal use of alternating current for generation and distribution, direct current still provides the most suitable source of power where adjustable speed or controlled torque characteristics are desired. D-c motors for machine tools, business machines, magnetic chucks, and separators, grinding machines, and elevators are typical examples of these d-c loads.

In comparison with other forms of converting equipment, the sealed-tube ignitron rectifier offers several distinct features for such service. The equipment is completely static, thereby eliminating special foundation requirements. The rectifier is quiet in operation which permits installation in locations where any appreciable noise would be objectionable. The over-all efficiency is high and practically constant over the entire load range. The no-load losses are small when compared with rotating equipment of like capacity. Rectifier equipments are factory-assembled, thereby minimizing installation time and expense.

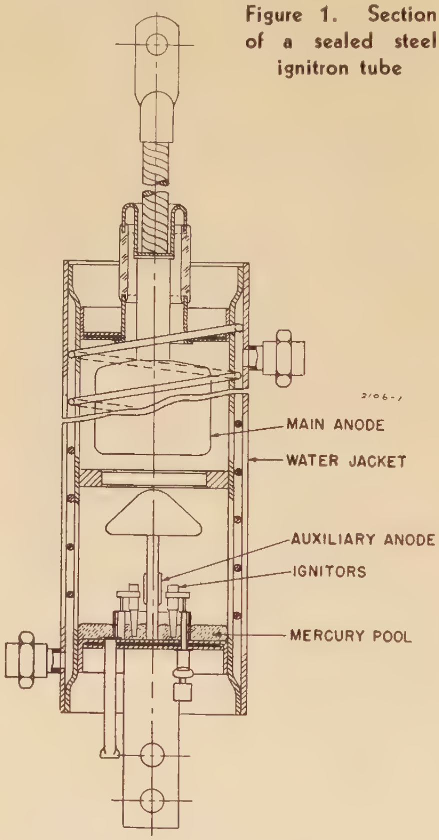

Figure 1. Section of a sealed steel ignitron tube

Tube Design and Operation Characteristics

Two sizes of ignitron tubes have been developed. The general tube design and construction are shown in Figure 1. The tube elements consist of a main anode, mercury-pool cathode, two ignitors, an auxiliary or holding anode spray, and deionization shields. Water jacket, flow spiral, and inner cylinder are of stainless steel.

Ignition[3] of a cathode spot is accomplished by passing an impulse of current from the ignitor to the mercury pool. Passage of current from the ignitor to the pool generates sufficient heat to break momentarily the contact of the mercury with some crystal in the ignitor. If the magnitude of the current, its duration, and the voltage gradient along the ignitor are sufficient, a cathode spot is established.

Figure 2 shows the variation in ignition voltage, current, and time for a typical ignitor, when supplied from the voltage of the main anode. When the ignitor is started at the beginning of the applied voltage wave, the current rise is slow, and the time required to reach the critical ignition value is long. When the applied voltage is high, as near the middle of the wave, the critical ignition current is quickly reached and the time correspondingly short.

Arc-drop voltages are given in Figure 3 in terms of anode current and outlet water temperature. These data were determined from cathode-ray-oscillograph measurements, while the tubes were supplying an inductive load of sufficient magnitude to insure essentially flat-topped current waves. Arc losses represent a design compromise between the minimum obtainable with no baffling and those with the shielding required to produce satisfactory freedom from arcback.

Cathode spots on a free surface of mercury become unstable when the instantaneous current decreases below approximately three amperes. To provide stable operation in this current range, an auxiliary anode has been added to the tube elements. This anode is usually excited from a low-voltage source inphase, or slightly ahead of the period of main anode conduction.

The tubes are water-cooled. Water-flow rates, cooling area, and temperature are determined by the requirements of removing the arc losses while maintaining vapor[4] pressure control. The outlet water temperature varies inversely with the load as is shown in the performance data of Figure 4. Maximum water temperatures are determined by the point at which the vapor pressure reaches arcback conditions. Minimum water temperatures are determined by the ability of the tubes to conduct current without "surging." This phenomenon is the result of insufficient vapor pressure to provide ions for the load-current demand. Momentarily con-

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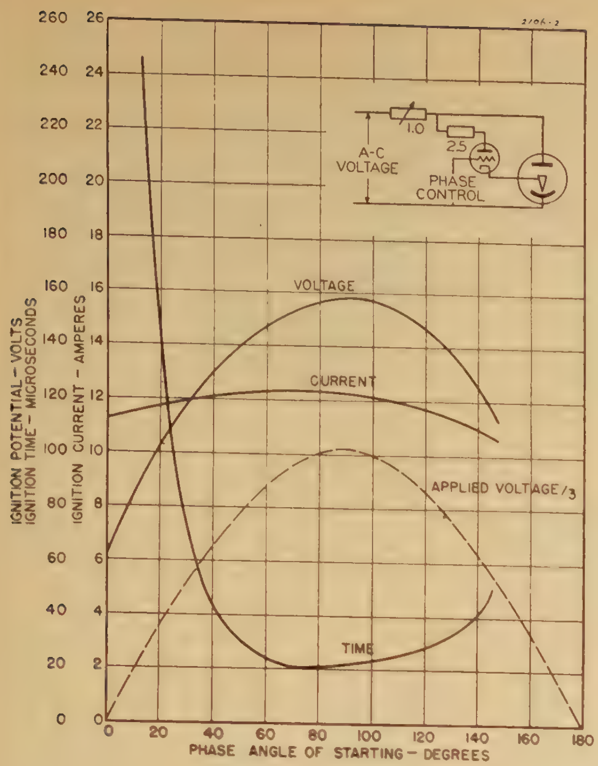


Figure 2. Ignitor characteristics

RECTIFIER KW RATING	IGNITRON TUBE NUMBER	IGNITRON TUBE AMPERE RATING	RECTIFIER POWER CIRCUIT
75	3	150	
100	4	150	
200	4	300	
300	6	300	
400	8	300	

Figure 5. Table showing typical power circuits and rectifying elements for 250-volt operation

duction ceases, and the rapid current changes in transformer and lead inductance generate high voltages. Surging is usually limited to the first few cycles of conduction and for the particular designs does not occur at full-load currents with water temperatures as low as ten degrees centigrade. At less than full load the temperature may be lower. Optimum operating temperatures are in the range of 30 to 50 degrees centigrade, since under these conditions the arc losses are nearly a minimum, and there is more reserve vapor pressure control to take care of current demands.

Ignitron tubes in common with most vacuum-tube devices have elements of relatively small mass. The time for averaging the heating effect of sustained current demands is therefore a matter of minutes rather than hours as found in more massive machinery. This time is adequate for clearing feeder fuses, starting motors and for the usual types of overloads that are encountered in service.

Rectifier Design and Performance

Power Circuits

The choice of rectifier power circuits permits the grouping of different numbers of tubes to obtain various rectifier capacities. Figure 5 shows some of the more common of these circuits, together with the number of rectifying elements required, and the nominal ratings which are obtained in 250-volt service. These nominal ratings are made on the basis of the continuous duty. The usual overload factors are 125 per cent for two hours and 200 per cent for one minute. For most applications the circuits in which several tubes conduct simultaneously are preferable. In such circuits the total load is divided. Arc losses are therefore a minimum, since the instantaneous tube currents are low, and current capacity is a maximum. In certain cases, however, ease of control makes circuits preferable in which each tube carries the full load current. Rectifiers[5] which supply field currents for synchronous machines are in this category. To provide rapid response for both increasing and decreasing line voltage, the rectifier must be capable of inversion as well as of rectification, and the control is simplified when circuits of the three-phase, quarter-phase, or six-phase type are used.

Two-wire service is readily obtained from the usual rectifier circuits. Three-wire service may be obtained in two ways. A balancer set capable of carrying unbalanced currents of the order of 25 per cent of the set rating is usually sufficient to provide the required neutral. Three-wire systems may also be obtained electronically through the use of a three-phase, double-way[6] circuit shown in Figure 6. A practicable installation may consist of two-wire circuits to carry the main part of the load and sufficient three-wire circuits to carry the unbalanced load.

Ignitor Excitation

Anode excitation[7] is the most direct method of establishing a cathode spot. In this case a portion of the load current is diverted through the ignitor by means of a

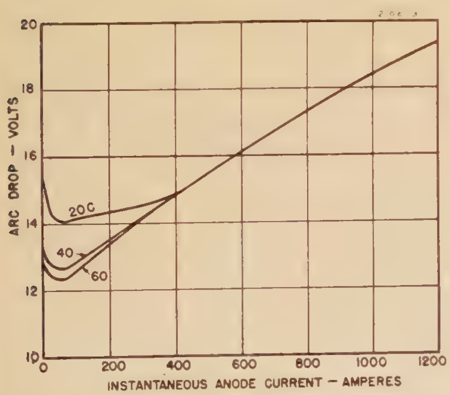


Figure 3. Arc-drop characteristics for cooling water temperatures of 20–60 degrees centigrade

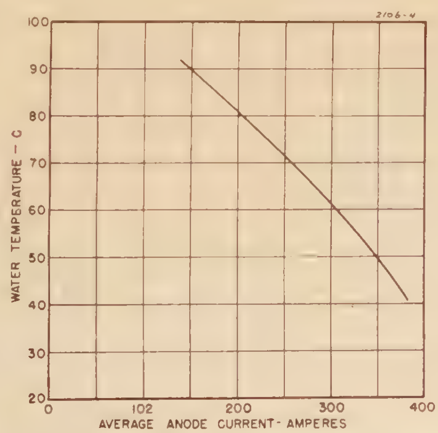


Figure 4. Arcback temperatures for operation at 250 volts in a three-phase single-way circuit

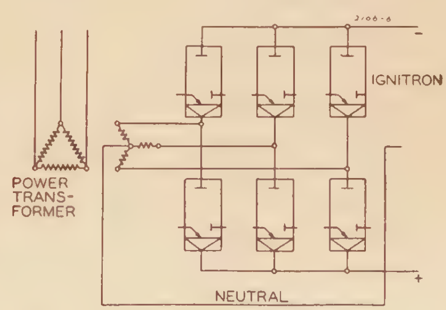
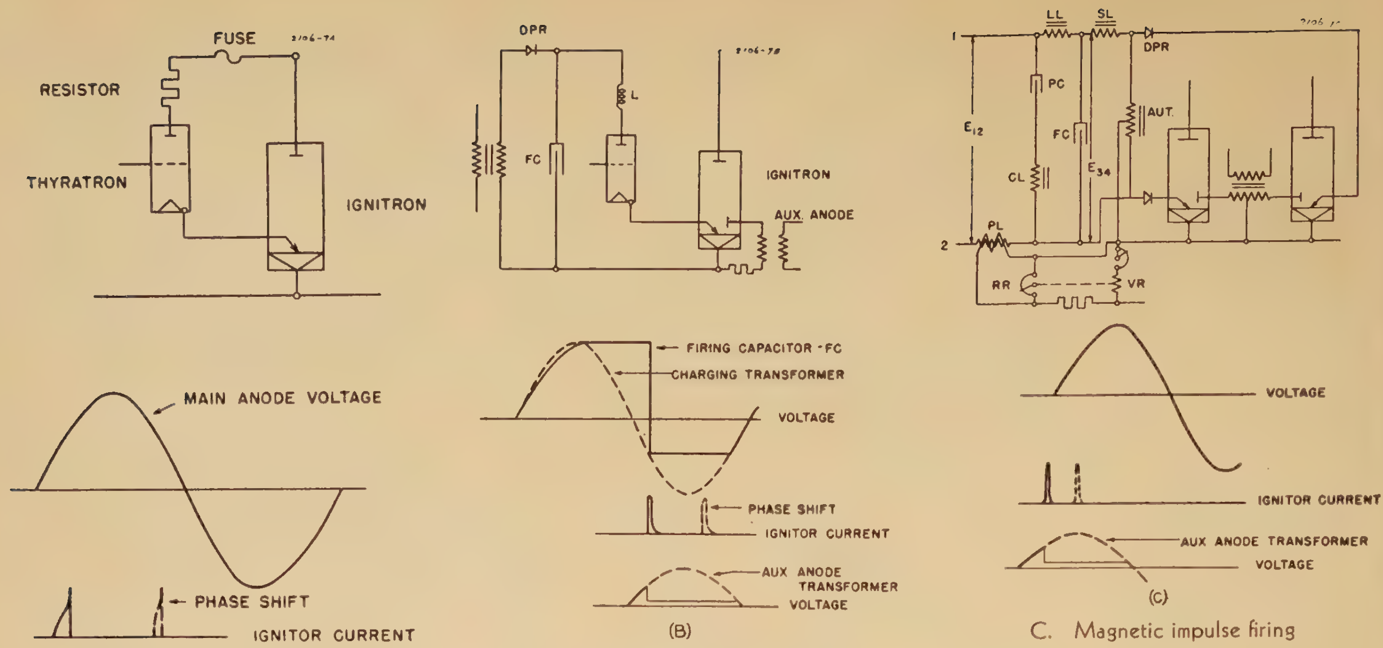


Figure 6. Three-phase double-way rectifier for three-wire service



A. Anode firing

B. Capacitor firing

C. Magnetic impulse firing

Figure 7. Ignitor excitation systems

suitable thyratron control tube. Establishment of the main arc effectively shorts the excitation system and thereby limits the average ignitor current to practicable values. This system is shown schematically in Figure 7A. The anode firing excitation system has two limitations. When the rectifier is feeding a counter electromotive-force load, and the current is low, the anode currents tend to become discontinuous. The voltage which can be applied to the ignitor becomes the difference between the instantaneous transformer voltage and that of the counter electromotive-force load. This voltage difference is so low that either a relatively long period is required for the ignitor current to reach the critical ignition value or the counter electromotive-force voltage decreases sufficiently to provide the required ignition current. Momentarily, therefore, the rectifier ceases to deliver its normal output voltage. This effect can be corrected by providing sufficient series inductance in the d-c load to assure that the current is continuous. Even though the load current is continuous, it may drop below the critical ignition value. In effect the ignitor resistance is suddenly placed in the circuit. The resulting reduction in output voltage can produce a very annoying light flicker when lamps are part of the connected load.

One form of separate excitation is shown in Figure 7B. In this case a capacitor (*FC*) is charged through dry-plate rectifier (*DPR*) and discharged at the proper instant through a thyratron tube. Phase control is obtained in the usual manner through the thyratron grid. An inductance in the discharge circuit establishes the desired rate of rise in the ignitor current. The circuits are so designed that a relatively high peak current, approximately 25 to 30 amperes, is available prior to ignition. This current value is in excess of the usual requirements, but, since the available energy is fixed, a design factor must be included for any possible contingency. The duration of the impulse is approximately 500 microseconds and the average ignitor current one-half ampere. Losses are of the order of 100 watts per ignitor. The ignition current is of such short duration that the cathode spot is maintained for the remainder of the main-anode conducting period by an auxiliary anode. Average current in this circuit is of the order of two amperes and losses 50 watts.

The required peak excitation current may be obtained[8,9] magnetically as shown in Figure 7C. Operation of the circuit is as follows: The linear reactor (*LL*) has an iron core with an air gap and is designed to give a constant reactance up to rated voltage and frequency of the circuit. The saturating reactor (*SL*) is built with a closed high-permeability core and a winding which will draw a large magnetizing current at the saturating point. This peak of current occurs when the capacitor (*FC*) is charged to its maximum voltage. The capacitor discharges through the saturating reactor, giving an impulse of ignitor current. A dry-plate rectifier (*DPR*) prevents reverse current from flowing in the ignitor circuit. The autotransformer (*AUT*) allows one circuit to supply excitation power to two tubes, which are in 180-degree phase relation.

The phase position of the impulses

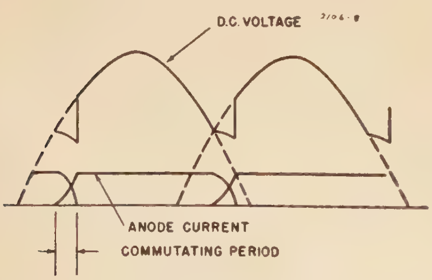


Figure 8. Diagram showing voltage loss due to commutation

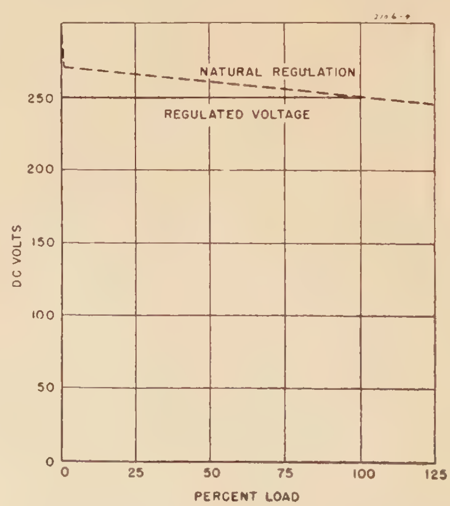


Figure 9. Typical output-voltage characteristics obtained by phase control

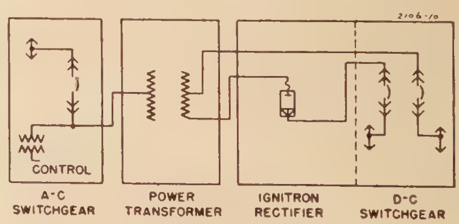


Figure 10. Line diagram showing rectifier component parts

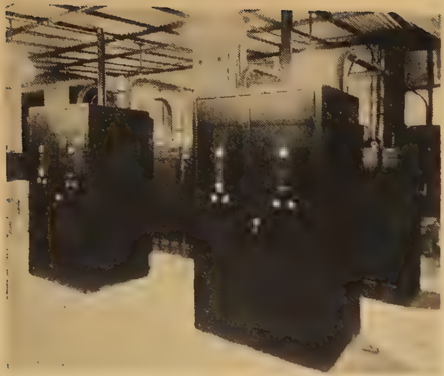


Figure 11. Metal-enclosed ignitron rectifier with automatic d-c switchgear for general purpose use

which establish the cathode spot are controlled by shifting the supply voltage (E_{34}) to the excitation circuit. This is accomplished by means of the network, consisting of reactor (*CL*) and (*PL*), and capacitor (*PC*). The reactor (*PL*) is capable of gradual change in reactance over a 10 to 1 range by means of d-c saturation. The reactor (*CL*) and capacitor (*FC*) operate with a circulating current larger than that drawn by the excitation circuit and act as a power source for this circuit. The phase position of the voltage (E_{34}) can be adjusted by controlling the point of saturation in the phase-shifting reactor. In this manner a 40-volt-ampere d-c source may be used to vary the firing point of the ignitor over an angle of approximately 50 degrees.

Fault Protection

Fault protection must be provided for a-c and d-c short circuits and for tube arcback conditions.

A-c protection for the transformer circuits is taken care of most satisfactorily by means of circuit breakers. The interrupting capacity of such a breaker is determined by the system capacity from which power is obtained.

The d-c protective equipment should provide for the usual functions of overload

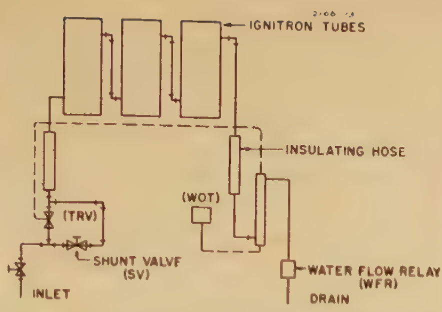


Figure 13. Water-cooling system of a 75-kw ignitron-tube rectifier

and short circuit. Again breakers are usually preferable, at least for the main feeders.

In spite of almost complete freedom from arcback when circuit, tube, and load conditions are properly co-ordinated, provision must be made for the possibility of arcback. Anode fuses of proper characteristics and capacity have proved satisfactory for this service. Differential protection can be obtained by the co-ordination of the tube current-carrying capacity with the transformer current-limiting reactance. The proper relationship of these two factors permits the remaining tubes in the circuit to carry the transformer short-circuit current without arcing back, for the eight or ten cycles required for the fuse to open. Likewise d-c short circuits may be cleared with the rectifier remaining available for service.

Voltage Control and Regulation

Regulation in rectifier circuits is due primarily to the voltage lost during the commutation period. At this time two anodes are conducting simultaneously, and the output voltage is the average of the instantaneous voltages. These conditions are shown in Figure 8. The voltage loss[10] during the commutation period is

Voltage drop $= PFLI$

where

P = number of phases or anodes which commutate consecutively

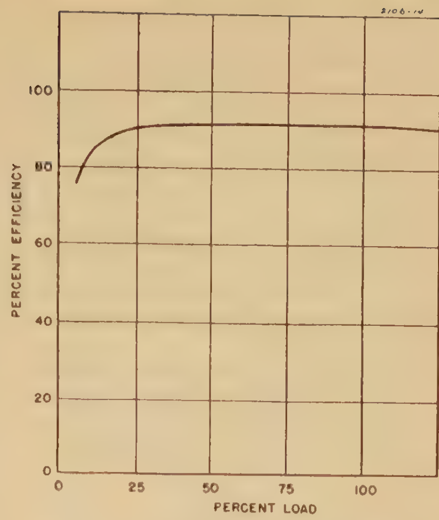


Figure 14. Over-all efficiency for a 250-volt 75-kw ignitron rectifier

F = system frequency

L = commutating inductance of one phase in henries

I = d-c current at the beginning of current transfer

The commutating reactance is that of any two phases whose anodes conduct in turn. It may be determined by shorting the transformer primary and applying excitation current to the secondary phases.

Rectifiers of this type usually have voltage regulation of six to seven per cent. This regulation is often greater than is desirable. To mitigate this effect, as well as compensate for variation in the voltage supply to the rectifier, the phase of the ignitor impulse is varied with respect to the positive half cycle of anode voltage to control the starting point of the anode current.

For magnetic excitation circuits, the simplest method of controlling the rectifier output voltage is to vary the d-c saturating current in accordance with the variation in the d-c output of the rectifier. In Figure 7C the voltage sensitive element (*VR*) of a torque-type regulator is connected across the d-c voltage to be regu-

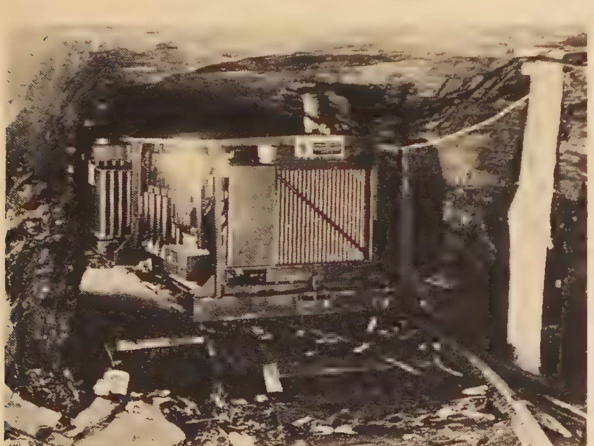


Figure 12 (left). Installation of a portable ignitron rectifier for mining service

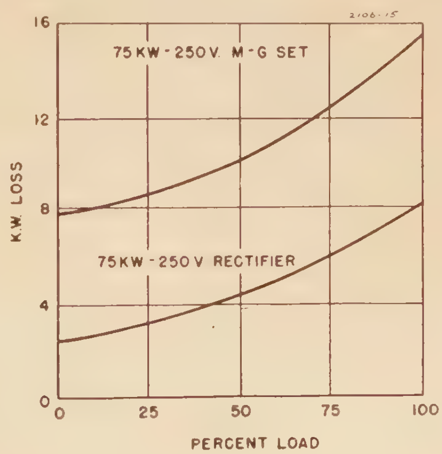


Figure 15 (right). Comparison of motor-generator and ignitron-rectifier losses

lated in series with a reactor and a variable resistor. The regulator rheostat element (*RR*) is connected in parallel with the saturating windings of the phase shift reactors. The voltage drop across the rheostatic element varies from a maximum to zero, thereby varying the current in the saturating winding of the reactor (*PL*). Parallel operation of one or more rectifiers can be obtained by use of an equalizer bus between the regulating elements of the various units.

Figure 9 shows a typical characteristic in which the rectifier voltage output is regulated from zero to full load. Beyond full load the output voltage follows the natural regulation.

Where extremely rapid response is desired, such as in the case of an electronic exciter operating to control the output voltage of a synchronous condenser, an electronic regulator[5] may be used.

Equipment Design

In general, the equipment design allows complete factory assembly of the units. The tubes, excitation, and control form one unit; the transformer another; and the required switchgear a third. A typical rectifier equipment is shown on the line diagram of Figure 10 and consists of the following components:

(*a*). Metal-enclosed manually or automatically operated a-c switchgear.

(*b*). Main, interphase, and control power transformers which are oil- or Pyranol-filled, natural or forced air-cooled, depending upon location and service.

(*c*). Metal-enclosed water-cooled ignitron mercury-arc rectifier with manually or automatically operated d-c switchgear.

Figure 11 shows a rectifier installation that is suitable for general purpose use such as lighting, power, or elevator loads.

Mine-service rectifiers require special designs in that the available head room is limited. Figure 12 shows a typical unit in which the rectifier, the transformer, and the switchgear form an articulated train. The cars are sufficiently short to permit moving around the usual track curves encountered in mine service. Portable rectifiers are particularly adapted to mine service, because they permit movement to new load centers as the coal is progressively removed.

Application

Rectifier ratings and the number of units required depend upon the application. In essential service it is considered good practice to have in reserve a stand-by unit which will permit the largest rectifier in service to be removed. Where requirements are less severe, and where the units may be removed for maintenance and inspection, rectifiers may be used to full capacity.

Elevator loads, particularly in hotels, apartment houses, and office buildings provide an excellent example of the essential service type of load. For reasons of safety such transportation must continue in operation. A satisfactory solution of the problem is to provide two complete and independent rectifiers with automatic change-over in case of d-c voltage failure. Simple switching provides preselection of the normally operating and reserve rectifiers, and the order may, of course, be periodically changed to equalize the use of the two units.

Tap water is usually used for removing the rectifier-arc losses. Full-load arc losses in 200-kw 250-volt rectifiers are approximately six per cent. Hence the transfer of this loss into the water rather than into the air is desirable, particularly in locations having limited ventilation facilities. The cooling water is not contaminated in passing through the tube jackets, and may be used in plant processes. Figure 13 shows the usual arrangement in which water is supplied to the rectifier at line pressure and passes through regulating valves to the tube jackets. Valve *SV* is a shunt valve used to adjust minimum flow. Valve *TRV* is controlled thermostatically and regulates the water flow in response to load. Thermostat (*WOT*) protects against water over temperature while the water flow relay (*WFR*) will remove the rectifier from operation on loss of cooling water flow. The flow relay utilized depends for its operation upon a physical displacement requiring both hydraulic pressure and water flow.

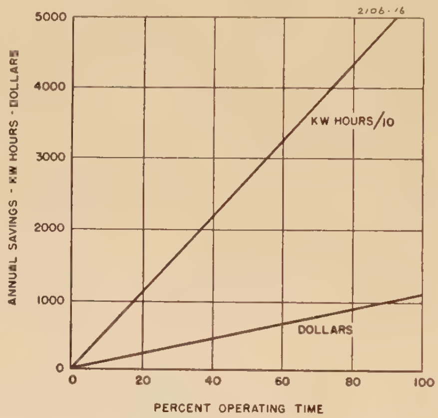


Figure 16. Annual savings for a 75-kw 250-volt rectifier

In general, most waters which are suitable for industrial or public use are satisfactory for cooling requirements. Waters which readily deposit lime or silt are to be avoided. The stainless-steel jackets are resistant to most corrosive elements, and the usual temperature of operation is not conducive to lime deposits. Stainless steel, however, is subject to slight corrosion by waters containing chlorides. Generally waters containing below 20 parts per million are satisfactory. Most public water supplies do not contain more than 8 to 15 parts per million.

Where local water conditions are unsatisfactory for direct use, heat exchangers may be employed. For mine service, air-to-water heat exchangers have been found to be the more practicable. Mine waters, in general, are very corrosive and very dirty. Air temperatures, on the other hand, are relatively constant and sufficiently low to make air-to-water heat exchangers attractive.

Maintenance

Rectifier maintenance outside of the usual inspection which should be given any electrical apparatus is primarily a matter of tube renewals. The number and frequency of such renewals depend upon the ultimate tube life. This ultimate life logically seems to depend upon the ignitor or loss of tube vacuum. To provide for the former, sealed tubes have two ignitors, one of which is held in reserve. Actually, examination of ignitors which have operated for two to three years shows that no appreciable erosion has taken place. Experience over a four-year period indicates there is no impairment in the tube vacuum.

A direct approach to the problem of maintenance is to evaluate the annual savings in power cost resulting from the higher efficiency and lower no-load losses of rectifiers. Figure 14 shows the over-all rectifier efficiency, and Figure 15 the kilowatt loss of a typical 75-kw motor–generator set and an equivalent ignitron rectifier at various loads up to full rating. Between 20 and 80 per cent of rated load, the difference in losses varies from 5.5 to 6.8 kw. Since the usual load characteristics are such that the load on either a motor–generator set or a rectifier will rarely lie outside of this range, the annual savings in kilowatt hours obtained by using a rectifier can be approximated very closely by multiplying the number of hours in service by 6.1. The gross saving depends upon the cost of electrical energy. Although power costs vary considerably, rates of one to three cents per kilowatt hour are representative. Figure 16 shows the annual saving on the basis of a power cost of two cents per kilowatt hour in

terms of the percentage of time the set is in operation. In general the average savings exceed the average replacement cost.

Conclusion

The design and application of rectifier equipment using sealed ignitron tubes has involved the solution of many new and interesting problems. Light flicker, control, voltage regulation, the problems of excitation, overload, and arcback protection have necessarily formed a part of the evolution of this type of rectifier equipment. The basic problems have been solved, and it is believed that the continuous and reliable operation of the sealed steel-tube rectifiers will demonstrate that these electronic devices have the same high standards of performance as the older forms of conversion equipment.

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Practical Design of Counterpoise for Transmission-Line Lightning Protection

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I. Introduction

EXPERIENCE has shown that preventive lightning protection, involving the installation of overhead ground wires and the co-ordination of tower-ground resistances with insulation level, can be quite effective in preventing flashovers due to lightning. Studies presented in an earlier paper[1] indicated that the efficiency of preventive protection can be made practically 100 per cent for high-voltage lines having insulator flashover levels of 1,600 kv and above. If the flashover level is reduced, the efficiency of protection becomes less.

Tower-ground resistance directly affects the protection efficiency obtained, and for satisfactory line performance resistance usually must be reduced below the values obtained with tower foundations alone. Buried counterpoise offers a practical means for accomplishing this, and, although many papers have been written on the theoretical aspects of the counterpoise, there is very little published on the problems encountered in actual installations.[2] This paper gives some methods that have been used in the installation of buried counterpoise at approximately 1,800 steel towers of the Pennsylvania Water and Power Company, as well as some of the results obtained.

When a counterpoise installation was contemplated, it was first necessary to determine the level of tower-footing resistances theoretically desirable. Next it was estimated how closely this objective could be approached and the amount of counterpoise conductor required. The data and methods presented are not sufficiently comprehensive to be applicable to all cases in all localities, but they may prove useful as a guide in making similar installations.

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II. Determination of the Optimum Tower-Footing Resistance

Experience has shown that in most cases flashovers will not occur at towers where the footing resistances are kept below the value indicated by the relation[3]

$$R = \frac{V}{I(1-CF)}$$

where

R = Tower-footing resistance
V = Impulse-flashover level of line insulation
I = Maximum natural tower lightning current
CF = Coupling factor between overhead ground wires and line conductors

This equation fixes the upper limit of footing resistance for any assumed value of tower current I, I usually being taken in excess of any tower currents that have ever been measured in the field.

Towers have suffered flashovers when the footing resistances and tower currents were appreciably less than the upper limit set by the above expression.[1,4] This has occurred more frequently than can be attributed to freak conditions. Apparently there is an impedance voltage drop in tower structures and grounding systems which accompanies heavy lightning discharges and which causes flashovers when the ohmic resistance drop appears low. As a practical consideration the effect has been evaluated in terms of tower currents.

For instance, on a 66-kv line having eight $4^3/_4$-inch insulators the upper limit for tower-footing resistance is obtained by substituting in the preceding equation

$$R = \frac{V}{I(1-CF)} = \frac{635{,}000}{150{,}000(1-0.25)} = 5.6 \text{ ohms}$$

It is assumed that 150,000 amperes is approximately the largest tower current that has ever been measured under natural lightning conditions. It would be logical to expect that towers having footing resistances of this level or less would not be subject to flashover; yet it has been found that flashovers have occurred on towers having footing resistance of this order of magnitude, when the indicated tower currents have been only about 60,000 amperes.[1] This leads to a modification of the desired level of tower-footing resistance based on economic considerations. With insulator flashover levels of about 600 kv, footing resistance can be considered the controlling factor for indicated tower currents up to only 60,000 amperes. Due to the uncertain

Table I. Optimum Tower-Footing Resistances for Several Transmission Lines

Line Operating Voltage (Kilovolts)	Suspension Insulator Units	Nominal $1^1/_2$x40-Microsecond Flashover of Insulators (Kilovolts)	Optimum Tower-Footing Resistance (Ohms)
66	$7\text{-}4^3/_4$ in.	565	5
66	$8\text{-}4^3/_4$ in.	635	6
132	$12\text{-}5^1/_4$ in.	1,015	10
220	$20\text{-}5^1/_4$ in.	1,605	16

accuracy of tower-current measurements, it has been suggested that tower currents measured by surge-crest ammeter links mounted on tower legs be multiplied by a factor of 2 to correct for the currents carried by cross members of steel towers.[5] This gives a maximum safe tower current of about 120,000 amperes and a practical working value of about seven ohms for the optimum tower-footing resistance on an actual transmission line having an insulator flashover level of 635 kv. As all the factors in the equation are not known exactly under natural lightning discharge conditions, it has been found that a good working rule for the desired footing resistance is that it shall be not more than one ohm for each 100 kv of insulation flashover level, using the $1^1/_2$x40 microsecond value. This is referred to as the optimum tower-footing resistance.

Some examples of optimum tower-footing resistances are given in Table I.

III. Selection of Counterpoise Length

Usually counterpoise is installed principally as a convenient auxiliary means for reducing tower-footing resistance. For this reason the amount of counterpoise

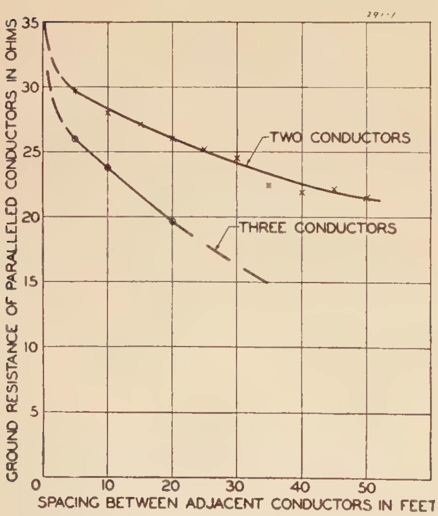

Figure 1. Effect of spacing on ground resistance of parallel buried conductors as determined by test

conductor used is that required to bring the ground resistance as close as practicable to a desired level. There are some notable exceptions to this principle. The section of the Boulder Dam–Los Angeles lines in high resistance territory[6] and the High Knob section of the Wallenpaupack Tap[5] are published instances of effective counterpoise installations where ground-resistance criteria do not apply. It is significant that in both of these cases lines operate at high voltage and have high insulation levels.

Based on traveling wave theory, Bewley and Hagenguth[7] have suggested that counterpoise conductors should be no less than 200 feet long. This minimum length has proved to be a satisfactory one in the field, from the viewpoint of both installation and performance. The maximum length that has been used on the 66-kv lines considered in this paper has been 400 feet where spans are greater than 800 feet, and continuous from tower to tower where the spans have been less than 800 feet. On 132-kv and 220-kv lines where the insulation levels are higher (see Table I), continuous counterpoise has been installed where ground resistances could not be reduced sufficiently by buried conductors 200 to 400 feet long. The basis for this difference in procedure for the two classes of insulation levels is the observed phenomena that the 66-kv towers flash over when low measured ground resistances are due to good grounds at some distance from the tower bases,[1] and that the higher-voltage lines can apparently withstand heavy discharges even though the necessary low ground resistances are obtained only by counterpoise extending for some distance from the tower foundation.[5,8]

IV. Selection of Number and Spacing of Counterpoise Conductors in Parallel

Dwight[9] has published formulas for calculating the ground resistances of buried conductors, and they show that the greater the spacing between parallel wires the lower is the ground resistance of the system.

To indicate the practical limits for spacing, a set of measurements was obtained with seven 400-foot buried lengths of bare solid copper rod of one-fourth-inch diameter. The conductors were laid in straight parallel lines, 18 inches below the surface of the ground, five and ten feet apart. After a suitable weathering period, the resistances to ground of the individual conductors were 37, 37, 35, 35, 35, 30.5, and 32 ohms. The re-

Table II. Reduction of Ground Resistance Due to Addition of Third Counterpoise Conductor Midway Between Two Others

Distance Between Two Outer Wires (Feet)	Ground Resistance in Ohms: Two Outer Wires	All Three Wires	Per Cent Reduction Due to Third Wire
10	28	26	7.1
20	26	23.8	8.5
40	21.8	19.7	9.6

sults plotted in Figure 1 emphasize the fact that two or more wires in parallel with reasonable spacings cannot produce so low a resistance as might be expected from the common relation for parallel resistances.

Table II, based on the data of Figure 1, gives a comparison of the ground resistance of two parallel conductors with that of a set of three when a third conductor is installed midway between the original two. It can be seen that the third conductor reduces the ground resistance only slightly, but the reduction in the surge impedance would be greater theoretically than that in ground resistance.

Another investigation was made in the field to determine the relative merits of the installation of two counterpoise conductors parallel to each other on one side of a tower, as positions 1 and 2 in Figure 2B, and the installation of the two conductors on opposite sides of the tower, as in Figure 2A. Four wires, 540 feet long, were buried in the ground in the pattern shown in Figure 2B, with 26-foot spacing between parallel conductors. The ground resistances were as given in Table III, and show about a 25 per cent advantage for the arrangement of Figure 2A.

Based on field data, Table IV gives the order in which it has been found that

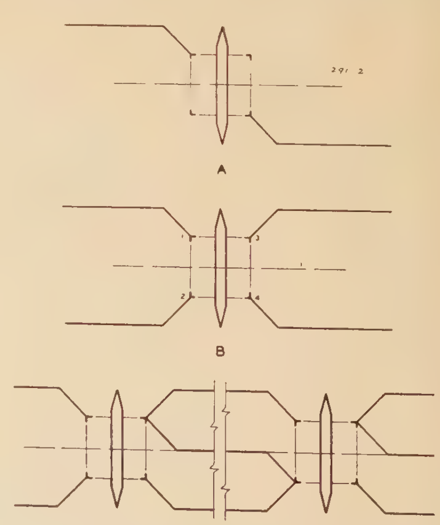

Figure 2. Arrangement of counterpoise conductors

Table III. Ground Resistance of Counterpoise Conductors at Test Installation Arranged as Shown in Figure 2B

Counterpoise Conductor Designations	Ground Resistances in Ohms
1	26.2
2	24.
3	25.3
4	26.9
1 and 2	18.5
3 and 4	18.8
1 and 4	14.1
2 and 3	13.2
All	10.1

counterpoise can be most effectively installed to reduce footing resistances to a desired level. The individual conductors are spaced as far apart as right-of-way restrictions allow, and the grounding conditions at each tower determine the type and extent of the counterpoise system to be installed.

V. Estimating Counterpoise Conductor Requirements

When tower foundations have been placed and a few weeks are allowed to elapse for the soil to settle around them, ground resistances are measured and used as indicators of the amount of counterpoise required. This procedure does not usually delay construction; when necessary, counterpoise can be laid and its resistance measured with a transmission line in operation. Incidentally, this fact has permitted counterpoise to be applied readily to the improvement of old lines. Tower-footing resistances have been found to be more reliable indicators of counterpoise requirements than spot soil-resistivity measurements, because of the abrupt changes in soil characteristics which occur in the territory under consideration. This territory varies from comparatively level ground, partly wooded, partly cultivated, up to hilly country where abrupt changes in elevation of a few hundred feet occur. Wherever rolling or hilly country is encountered, the soil consists of heavy clays liberally interspersed with broken shale or large boulders. In level country near sea level, the soil is generally sand or river silt.

Table IV. Recommended Order for Installation of Counterpoise Conductors

1. Two 200- to 400-foot conductors at opposite sides of tower (Figure 2A)
2. Four 200- to 400-foot conductors (Figure 2B)
3. Conductors extended to adjacent tower on one side, then to adjacent tower in opposite direction as required (see section III)
4. Third conductor laid midway between other two (Figure 2C)

It would be desirable to predetermine two characteristics of a counterpoise installation:

1. The levels to which ground resistances can be reduced from given initial tower-foundation resistances.

2. The amount of counterpoise conductor which will be required to attain the desired result.

No satisfactory method of doing either of these things was found until some actual field experience was accumulated. Based on data such as that given in section VII, estimates could be made of the average wire needed and final ground resistance to be attained for towers having foundation resistances in a given range. Even then, accurate estimates could not be made unless a large number of towers was considered, because the actual installation at a given tower frequently varied a good deal from the average result. Each tower and span was considered separately, taking into account situations in which long counterpoise conductors were required, and the installation would serve to improve conditions at both towers at the end of the span. In some cases it was found that certain towers would themselves require short counterpoise, while the neighboring tower would require a continuous one. An installation of this type, designed to make satisfactory the worse of two grounding conditions, would reduce the final ground resistance of the low-resistance tower much below the optimum value.

At those towers where large amounts of counterpoise conductor were called for, not more than three conductors in each direction from the tower were specified, because of considerations which were discussed in section IV.

VI. Ground-Resistance Measurements

A common method for making ground-resistance measurements is the three-electrode method.[10] With counterpoise conductors several hundred feet long, the required spacings of the two temporary probes used in the three-electrode method become impractically large. A simple and practical substitute for the three-electrode method is the direct reference method, using the completed transmission line and its grounds as the reference electrode.[10] The ground resistance measured by this method includes that of the reference electrode as well as that of the system being investigated. The ground resistance of an entire transmission line is usually only a fraction of an ohm, making the observed resistance the ground resistance of the counterpoise system for all practical purposes.

When applying the direct reference method, the counterpoise conductors are disconnected from the tower at which it is desired to obtain measurements and also from the next adjacent tower on each side, if the counterpoise is continuous. The counterpoise conductors are then electrically connected in parallel, and the resistance measured between their common point and the tower. The overhead ground wire serves to connect the tower to the rest of the system. It has been found by tests that the ground resistance of a counterpoise system and a tower in combination is approximately the same as that of the counterpoise system alone. The reason for this phenomenon is that the tower foundation lies within the extensive counterpoise system and does not contribute appreciably to the conductivity of the system as a whole.

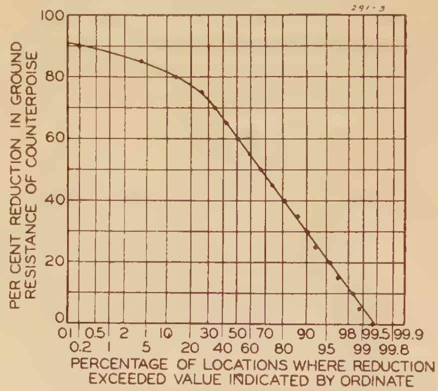

Figure 3. Reduction of counterpoise ground resistance due to weathering, at 483 tower locations

Counterpoise installed with a wire-laying plow

In this connection it is of some importance to note that the same effect is present when ground rods of moderate length are used at tower foundations. Due to the size of a tower foundation in relation to that of the rod and the small amount of additional soil contacted, the ground rod has been found to reduce ground resistances only slightly below that of the foundation alone. Exceptions occur in those special cases where a conducting stratum has been reached by rods of moderate length or very long rods have been driven.[11]

Practical considerations when making field measurements require that care be taken that corroded contacts do not cause inaccurate measurements. Defective electrical contacts may appear in the ground

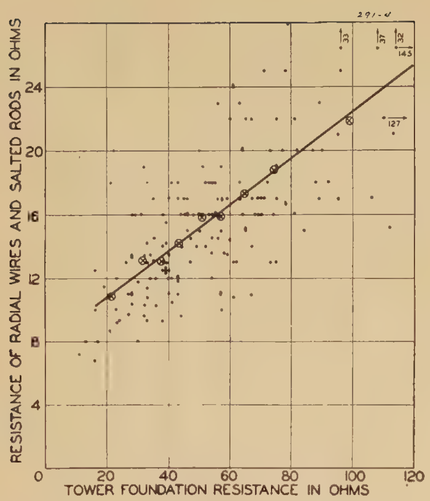

Figure 4. Ground resistances produced by four 50-foot buried radial wires terminating in salted rods at 171 locations on the Safe Harbor–Westport 220-kv line

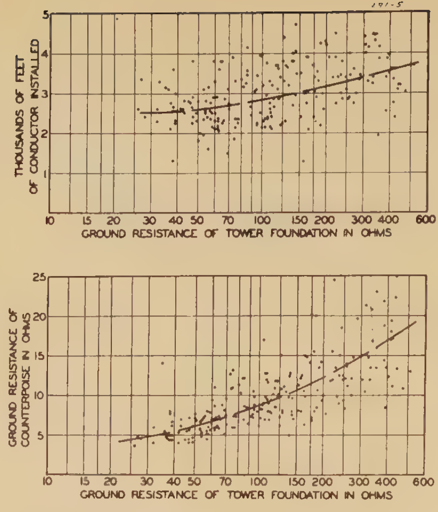

Figure 5. Counterpoise characteristics at 216 locations on the Holtwood–Coatesville 66-kv line

In many cases, counterpoise is continuous from tower to tower and serves to reduce ground resistance at two towers

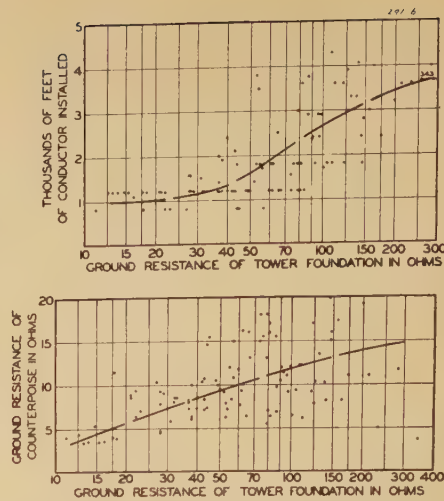

Figure 6. Counterpoise characteristics at 100 locations on the Safe Harbor–Perryville 132-kv line

The longer counterpoises are usually continuous and serve to reduce resistances at two towers

at bonded joints in the counterpoise conductors and also at the tops of towers where the overhead ground wires are supported.

VII. Field Data

Following the installation of counterpoise with a plow, there is a decrease in ground resistance during the ensuing period while the weather packs the soil into better contact with the buried conductor. This phenomenon has been found to be a highly variable one, indeterminate at any given location except by actual measurements of ground resistance. For instance, it can be seen in Table V that the variations in reduction have been from 0 to 91 per cent of the resistances measured at time of installation. After the initial weathering, further changes in counterpoise resistance have been found to be inappreciable. When making an installation, it has been the practice to allow for a reduction in ground resistance in order to limit the amount of wire used. In making this allowance, a balance was struck between an occasional installation of an excessive amount of counterpoise conductor as against the probable hazard of an occasional high footing resistance. In some cases where little or no reduction materialized, additional counterpoise was installed after weathering had occurred. Figure 3 can serve as a basis for establishing a working rule.

On the first line to which the principles of preventive lightning protection were applied, attempts were made to reduce the ground resistance of tower foundations by the addition of four 50-foot buried radial wires terminating in salted eight-foot ground rods. The results of this work are shown in Figure 4. In addition to the individual points shown on the graph, the encircled crosses each represent the average for 19 locations. This method of improving ground resistance was not satisfactory, because it did not reduce most of the tower-footing resistances below the desired level of 16 ohms. Subsequently footing resistances were improved by the addition of counterpoise conductor.

Figures 5 and 6 show the characteristics of counterpoise on two lines on which counterpoise alone was used to improve footing resistances. On the 66-kv line it was desired to obtain footing resistances below six ohms. On the 132-kv line the optimum value was ten ohms. As would be expected from the relative magnitude of the two desired final values, the economical maximum amount of wire which could be installed at a given location was reached more often on the 66-kv

Table V. Reduction of Ground Resistance of Counterpoise Due to Weathering at 483 Tower Locations

Per Cent Reduction $\left(1-\frac{\text{Final R}}{\text{Initial R}}\right)$	Tower Locations Number	Tower Locations Percentage	Percentage Exceeding Maximum Reduction of Range
0	3	0.6	99.4
1– 5	3	0.6	98.8
6–10	2	0.4	98.4
11–15	7	1.5	96.9
16–20	6	1.2	95.7
21–25	15	3.1	92.6
26–30	12	2.5	90.1
31–35	15	3.1	87.0
36–40	27	5.6	81.4
41–45	35	7.3	74.1
46–50	33	6.8	67.3
51–55	36	7.5	59.8
56–60	44	9.1	50.7
61–65	36	7.5	43.2
66–70	43	8.9	34.3
71–75	39	8.1	26.2
76–80	62	12.8	13.4
81–85	45	9.3	4.1
86–90	19	3.9	0.2
91	1	0.2	0

Table VI. Summary of Counterpoise Installations Where Ground Resistances Were Measured After Weathering Occurred

Line	Number of Tower Locations	Ground Resistances in Ohms: Tower Foundation Maximum	Tower Foundation Average	Counterpoise Optimum	Counterpoise Measured Maximum	Number of Locations Where Measured Exceeds Optimum	Feet of Conductor Installed Per Tower Location Maximum	Average
Safe Harbor–Westport–Takoma (220 kv)	116	400	150	16	31	52	2,900	1,071
Safe Harbor–Riverside (220 kv)	234	450	77	16	18	8	4,650	1,172
Safe Harbor–Perryville (132 kv)	167	343	58	10	20	72	3,000	1,388
Holtwood–York (66 kv)	151	525	137	6	20	110	2,100	1,570
Holtwood-Coatesville (66 kv)	216	511	147	6	25	167	2,400	1,612

line than on the 132-kv line. It must be borne in mind that the lightning performance of these two lines is very good even though the optimum conditions were not attained in many cases.

The wide scattering of points in Figures 4, 5, and 6 are attributed to the abrupt changes in soil conditions which were frequently encountered. Where salted rods were used, they frequently could not be driven to their full length of eight feet due to rocky conditions. Where counterpoise was used, lower resistances to ground were obtained in cultivated fields and pasture land than in woods, and in low sections, than on hills. As the counterpoise was laid, it frequently would pass from one soil condition to another, producing noticeably different results on one side of a tower from those produced on the opposite side. In spite of the large variations in the results obtained at individual towers having similar foundation resistances, data of the type given have been useful in estimating the average requirements of projected installations.

It has not been the practice to remeasure the ground resistance of counterpoise at every location after weathering has occurred. It has been measured only at those locations where the values at time of installation were greater than the optimum values. For this reason complete data cannot be given for all locations on every line. However, in Table VI an indication is given of the magnitude of reductions in ground resistance that have been obtained by the use of counterpoise. The criterion of the worth of preventive lightning protection is the outage record of the lines to which it has been applied. Such a record is given in Table VII, and counterpoise has played an important role in minimizing interruptions due to lightning.

VIII. Summary

1. There is apparently an optimum value of tower-footing resistance for a given level of insulation on a transmission line. This has been evaluated empirically as approximately one-hundredth of the kilovolt flashover value of the insulation.

2. Three parallel wires in a counterpoise system seem to be the largest practical number. Such an installation provides six electrical paths away from a tower foundation.

Table VII. Lightning Outages of Lines Having Counterpoise as Part of Preventive Lightning Protection

Line	Years of Record	Outages	Outages Per 100 Miles of Line Per Year
Safe Harbor–Westport–Takoma (220 kv)	10	1	0.11
Safe Harbor–Riverside (220 kv)	4	1	0.50
Safe Harbor–Perryville (132 kv)	7	1	0.42
Holtwood–York (66 kv)	6	2	1.46
Holtwood–Coatesville (66 kv)	4	3	2.55

3. The counterpoise system installed at each tower on a line must be graded to the conditions at the tower location if uniformly low ground resistances are to be obtained at reasonable cost. Uniform low ground resistances along a line are to be preferred to a uniform counterpoise system.

4. The ground resistance of a counterpoise system decreases for a period following installation due to settling of the soil around the buried conductors. After the initial weathering period, seasonal variations usually are small in the territory where observations were made.

5. The amount of counterpoise conductor installed and the final resistance obtained at towers having similar foundation resistance varied a good deal due to abrupt changes in soil conditions in the territory studied. In spite of the variations, the average results have been helpful in estimating conductor requirements for new work.

6. Preventive lightning protection, in which counterpoise plays a prominent part by reducing tower-footing resistances, is very effective in preventing flashovers on high-voltage lines.

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Electric Control for Steam Boilers on Diesel-Electric and Straight Electric Locomotives

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ELECTRIC control is a necessity in the operation of the modern steam boiler if the boiler is expected to have maximum efficiency, safety in operation, and constant operation at the various outputs required. Installation and operating conditions vary over a wide range; therefore, to meet these conditions the controls used differ in type and quantity in order to supply those actually needed for good operation, as well as those required to comply with rules and regulations under which the installations are permitted. Generally, the method of operation of such boilers can be classified in two categories, namely, automatic and semiautomatic, and these two classes will be discussed in this article.

Electric control is further separated into two component parts, where one group of items consists of the operating necessities for actual boiler operation, and the other of safety features required under legal regulations (including those needed for the safety of the equipment itself in case of failure of any major part).

Automatic-Control Boilers

Operating Necessities

To analyze the operation and use of each of the control items used for a complete automatic installation, it will be advisable to start with the basic boiler and add each electric item in the order of its use for normal operation.

Current for igniting the atomized fuel is generally obtained from high-voltage transformers or magnetos, with the majority of installations using the transformer, because of its ability to give a very hot spark for complete combustion. With transformer ignition a source of alternating current is required, and this is obtained from a suitable rotary converter where the necessary direct current is taken from the locomotive batteries or control source.

To provide constant ignition under all operating speeds of the boiler motor—including the off cycle of the boiler when no steam is being used—the converter is connected directly across the line and is controlled by the master control switch. The rotary converter serves another purpose also by supplying alternating current for the electric-eye operation, which will be discussed later under safety controls.

The pressure switch, operating directly from steam pressure, actuates the balance of the controls to regulate the required output. Where various output capacities are required, a multifinger switch is used to vary the motor speed in accordance with steam output and it automatically cuts in the motor-speed relays to keep the steam pressure up to the operating point.

As less steam is used, the pressure switch causes the contact fingers to operate in a definite sequence to slow down the motor, and thus only sufficient steam is made to replace that amount which is being used. When the steam consumption becomes greater than the output, the contact fingers operate in the reverse cycle and cause the motor to speed up in order that sufficient steam should be available for the requirements.

Connected in the oil line is a solenoid valve used to stop the flow of oil when such operation is required. The valve coil receives its current from the main relay of the motor control and is only energized when the relay is closed and the motor running. During the off cycles of the boiler the motor relays are de-energized; therefore, the oil solenoid valve is closed to prevent any oil from collecting in the combustion chamber which might cause a minor explosion when the fire again lights. This valve is also kept de-energized when the main motor is being run, only for the purpose of filling the boiler with water, and is accomplished by an interlock in the master control switch.

An important item in electric controls is the time-delay relay which has several functions during the normal operation.

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When the boiler is ready to run, and the proper switches have been turned to their operating position, the time-delay relay is momentarily energized, but the contacts remain closed for a predetermined length of time. Unless the oil ignites and keeps burning during this predetermined time, the contacts will open and shut the boiler down. If the fire continues to burn, then the relay coil will become energized through the electric eye or stack switch operation. During the off cycles of the boiler, it is also necessary to keep this relay energized in order that the motor will again start when steam is required, and this is accomplished by an interlock on the motor relay or a separate pilot relay. The time-delay relay is generally adjustable, so that the predetermined time setting can be adjusted to suit the conditions required for various sizes and types of boilers.

Safety Features

On any boiler installation safety control features must be included in order to comply with laws or regulations, and also to insure safety in operation of the boiler itself. To provide some means of stopping the boiler in case the fire does not properly ignite or goes out because of some defective part of the controls, some means must be provided to detect this condition, and transmit the proper impulse to the motor controls. Two methods are generally used, one being the photoelectric-cell type and the other a stack switch whose contacts are operated by a bimetallic helix.

The photoelectric-cell method consists of locating the photoelectric cell so that it is affected by the oil flame in the combustion chamber, causing current to flow to the amplifying unit. The rectifying and amplifying unit used in conjunction with the photoelectric cell includes a sensitive relay that remains energized whenever the proper flame is burning in the boiler. If the flame should go out while the motor is still running, the sensitive relay acts on the time-delay relay, which will stop the motor after the predetermined time lag. During normal operation, where the pressure switch stops the motor and cuts off the flow of oil, a pilot-relay interlock keeps the time-delay relay energized even though the sensitive relay contact is open.

Stack-switch control is similar to the photoelectric cell except that the contacts are opened and closed mechanically by the bimetallic helix which is, in turn, affected by the temperature of the gas surrounding it in the smoke stack. These

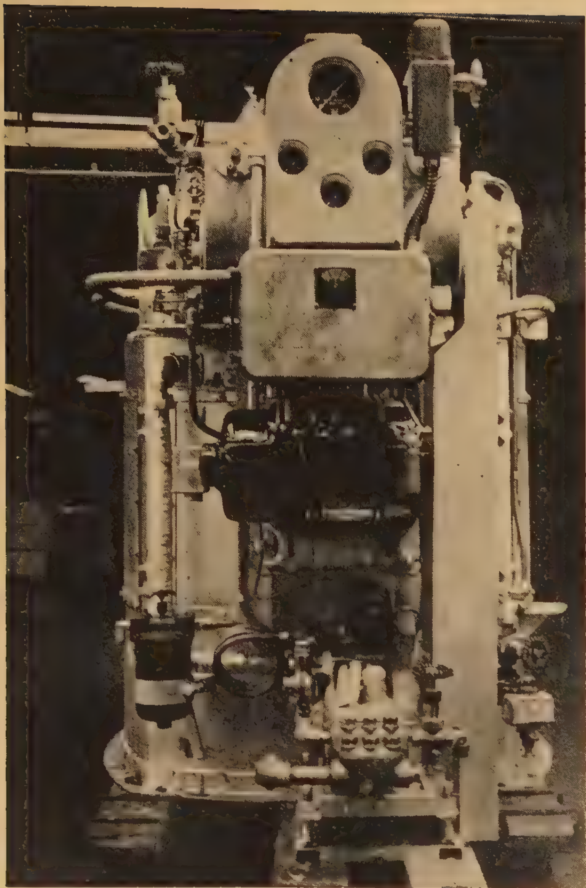
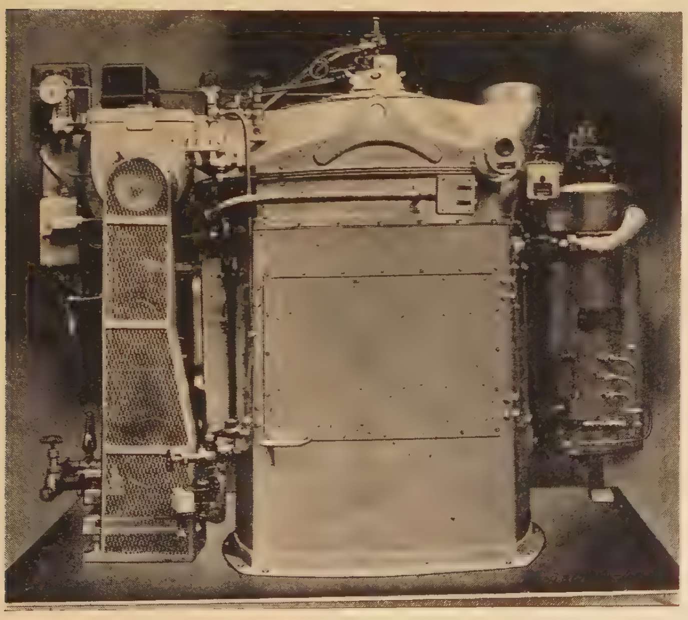


Figure 1. Front view of automatic steam boiler



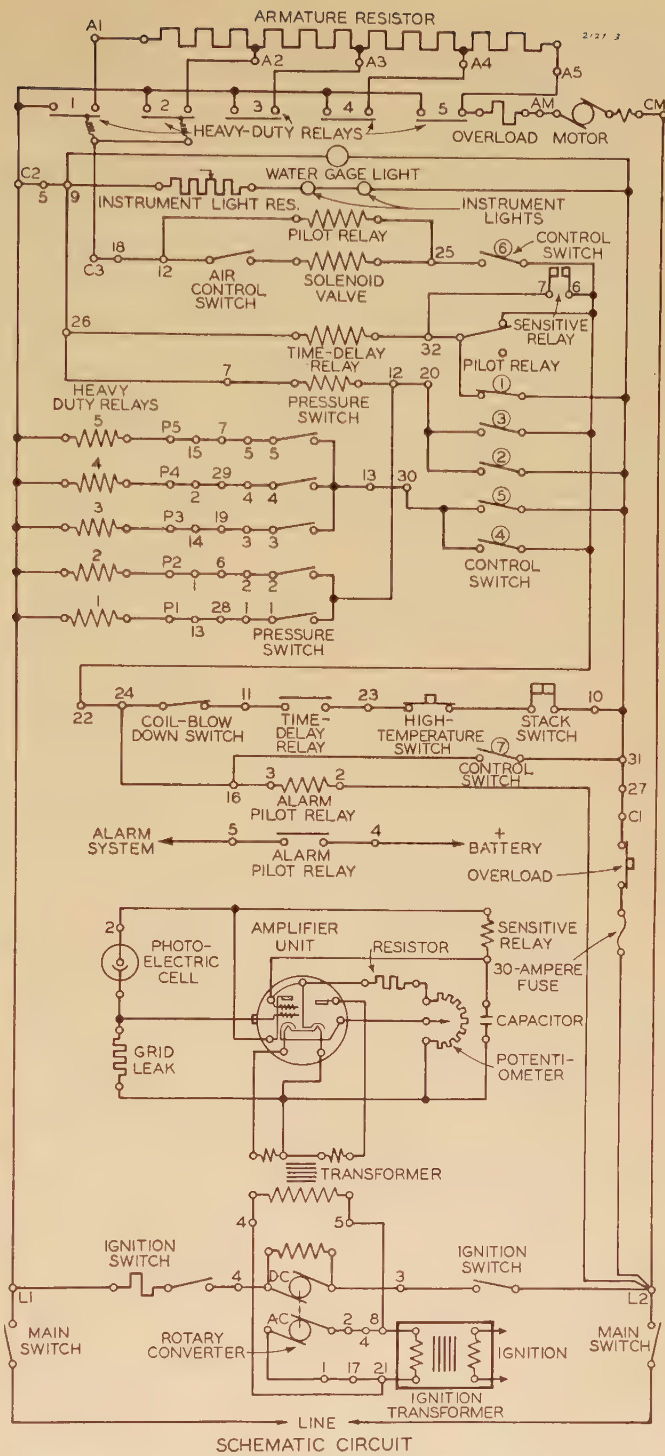


Figure 2 (left). Side view of automatic steam boiler

Figure 3. Schematic wiring diagram for described boiler

CONTROL SWITCH OPERATING POSITIONS

POSITION	1	2	3	4	5	6	7
TEST	●	●			●		●
OFF	●						
START			●			●	
RUN			●	●		●	

contacts are normally open and they close when a relatively low stack temperature has been reached such as one resulting from a continuous burning fire.

Where air, under pressure, is used to atomize the fuel oil, an air control switch is incorporated in the controls to insure that sufficient air of proper pressure is fed to the oil nozzle, so that the combustion will be complete and retain a clean fire. The air control switch is usually set to open the control circuit when a reduction of air pressure in the feed line reaches a point that will affect the combustion quantity of stack smoke and efficiency.

To protect against excessive high steam temperature, a high-temperature switch is incorporated in the control circuit to open the motor circuit when the temperature limit is reached. One type of switch uses an alloy that melts at this temperature and, in doing so, opens a spring set contact. To reset, the molten alloy must first cool and harden in order to hold the contacts closed for further operation of the motor controls.

A periodic blowdown of the coils is necessary to remove sediment and other foreign materials from the coils and pipe lines connected with them. To facilitate this operation a coil blowdown valve is

installed in the water intake line, and incorporated with it is an electric push-button assembly. When the valve is open the electric circuit is open, so that the boiler cannot run while steam is being blown back, and the boiler cannot be restarted until the valve is fully closed.

To guard against excessively high stack temperature, which is an indication of some faulty adjustment or badly sooted coils, a stack switch is located in the smoke stack, and its contacts are operated by a bimetallic helix. The high temperature contacts are normally closed and, once opened, have to be reset manually and only after the temperature has decreased sufficiently to enable the contact mechanism to re-engage. Installations that use a stack switch instead of photoelectric cell for a low fire control have both low and high temperature contacts operated by a common helix in one housing.

Alarm Mechanism

Audible or visual alarms are used with boiler controls, depending on the type of installation and rules of the operator. In general, the alarm circuit consists of a suitable relay whose contacts, when closed, ring a bell or light a series of lamps, or both. The alarm relay is connected into the boiler control circuit so that the opening of the contacts of the various safety features will not only shut down the boiler, but it will also give the designated alarm.

Figure 1 shows the front view of a typical automatic boiler equipped with controls as mentioned above. The multifinger pressure switch is shown to the right of the gauge box and in front of the air intake. Control cabinet directly below the gauge box contains the starting and speed-selection switch, time-delay relay, pilot relay and terminal-connection panel. The master motor for driving the blower wheel, water pump, and oil pump is shown mounted directly beneath the control cabinet. At the lower right-hand corner is shown a cutout box in which is located the electric push button used in conjunction with the coil blowdown valve.

Figure 2 shows the side view of the same boiler. At the upper left position is shown the side view of the multifinger pressure switch as previously described. To the right of the pressure switch is shown the ignition transformer and the high-voltage wires connected to the spark plugs which are near the top of boiler. The oil solenoid valve is shown directly at the top of boiler above the peep sight case. Just below the smoke-stack connection is shown the stack switch, mounted in the smoke hood, and to its right the high-temperature switch is mounted in the steam line.

Figure 3 is a schematic wiring diagram used in conjunction with this type of boiler and shows the connections between the various electrical operating controls and safety features.

Semiautomatic-Control Boilers

Semiautomatic-control boilers are essentially the same as the fully automatic-control boilers except for a few operating differences. The pressure switch is of the single-finger type, which opens and closes in accordance with the steam pressure selected but has no effect on the speed of the driving motor other than start and stop. Speed selections of the motor are made with a manual selector switch which connects to the motor various amounts of resistance, depending on the position of the switch. By this means the operator adjusts the motor speed, so that the steam output of the boiler will be satisfactory for the load it has to carry. Where motors used are of such size that motor starters are required, the necessary step control relays and starting resistance are needed in addition to the manual selector switch for actual speed-control setting.

Controls of this type are naturally limited to installations where the load is relatively constant for considerable lengths of time, or where an attendant is readily available to change the motor speed setting as often as necessary.

Electrical controls for steam boilers play such an important part in their operation, that any amount of time spent in developing the correct type is warranted by the practical results obtained. Full-time operation at maximum output and highest efficiency can be had only with electric controls that perform their respective functions in the way intended. The items mentioned in this article have been selected, from past experiences, to be necessary for proper operation and complete control of all functions of the boiler, including the safety requirements.

Electronics of the Fluorescent Lamp

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THE fluorescent lamp is one of the recent and rapidly growing additions to the field of electronic devices. In order to understand properly the characteristics of the fluorescent lamp, it is necessary just as with other electronic devices, to know something about the fundamental principles of operation. It is the purpose of this paper to describe the operating principles of this type of lamp, both as an electrical conductor and as a radiation generator. Following a brief outline of the properties of radiant energy as they concern lamp design, a description is given of the phenomena occurring in the separate regions of the lamp. The final section of the paper is concerned with the over-all characteristics of the lamp considered primarily as a circuit element.

The purpose of any electric lamp is to convert electric energy into radiant energy. The usefulness of a lamp depends not only upon the efficiency with which it converts the input energy into radiation, but also upon the suitability of the produced radiation for the particular application involved. The following description of radiant energy is presented to indicate requirements placed on a lamp as regards radiation output. In order to indicate the similarity of lamps to other electrical equipment, radiation is described in terms of frequency rather than by the more conventional wave-length designation. For the benefit of those more familiar with the wave-length description, important values are given in both units.

Radiant Energy

To be useful as light, the radiant energy produced by a lamp must fall within the region of the spectrum to which the human eye is sensitive. This region is shown in relation to the remainder of the electromagnetic spectrum by the chart of Figure 1. Although the ultraviolet and infrared regions of the spectrum immediately adjacent to the visible are not directly useful for illumination, they are increasing in importance for other uses, and the term lamp has been extended by common usage to include sources of radiation in these regions of the spectrum. The range of frequencies covered by commercially available lamps is shown in the expanded portion of Figure 1.

It is desirable to control the processes of energy conversion within a lamp so that the proper frequency of radiation is obtained for each intended application. This may be illustrated for visible light by the curve of Figure 2, which shows the average response of the light-adapted human eye as a function of frequency. This may be compared with the response of a radio receiver tuned to a frequency of 540×10^{12} cycles per second, corresponding to a wavelength of 5,550 angstrom units (A). It is apparent that the greatest possible light-producing efficiency would be obtained with a lamp which converted all the energy input into radiation at this frequency. Such a lamp would be a green light source having an efficiency of approximately 620 lumens per watt. However, the usefulness of such a lamp would be limited because of its color. When a lamp produces radiation at any frequency other than 540×10^{12} cycles per second, its luminous efficiency is necessarily lower than the above value. For example, a lamp which converted all the input energy into radiation within the visible region having the same spectral distribution as average daylight would have a luminous efficiency of only 220 lumens per watt.

In the incandescent lamp, the spectral distribution of the output radiation is continuous throughout the spectrum and is controlled by the operating temperature of the filament. Since the energy distribution is continuous, it is not possible to confine the output to a narrow range of frequencies, except by filtering out and thus wasting the undesired radiation. In a gaseous discharge lamp the frequency of the output radiation is fixed by the kind of gas, each gas having certain characteristic frequencies which it can radiate. The relative amounts of energy radiated at the different frequencies can be controlled by the type of discharge. A good example of this method of control is given by two types of mercury lamps: in the low-pressure germicidal lamp most of the radiation from the mercury is concentrated in the short-wave ultraviolet, while in the high-pressure capillary lamp a major portion of the radiation is produced in the visible and long-wave ultraviolet regions. In the fluorescent lamp the output frequency is controlled by the choice of the fluorescent coating, as will be described later in this paper. However, the operation of the fluorescent lamp also involves a gaseous discharge in which, as in the simple discharge lamp, the most important controlling factors are the nature of the gas and the type of discharge. The electronics of the fluorescent lamp are concerned with the operation of this gaseous discharge, both as a circuit element and as a radiation generator.

Principle of the Fluorescent Lamp

The process by which the fluorescent lamp converts electric energy into visible radiation is analogous to that of two energy converters connnected in series. The first of these is a gaseous conduction lamp which is designed to convert electric energy into ultraviolet radiant energy. This lamp usually takes the form of a glass tube having a length of from 10 to 50 diameters. Within the tube is a small drop of mercury producing a mercury vapor pressure corresponding to the temperature of the tube wall. Also present is a low pressure of argon gas. Specially designed electrodes at each end of the tube are connected to the external circuit by means of vacuum-tight glass-to-metal seals. In operation this constitutes a low-pressure mercury vapor discharge lamp which can be made to convert 50 per cent or more of the electric energy input into ultraviolet radiation of wavelength 2,537 angstrom units corresponding to a frequency of $1,182 \times 10^{12}$ cycles per second.

The second energy converter in the fluorescent lamp is a frequency changer which converts the $1,182 \times 10^{12}$ cycles per second radiation into visible radiation with frequencies between 750 and 430×10^{12} cycles per second (4,000–7,000 angstrom units). This frequency converter consists of a coating of fluorescent powders which is applied to the inside surface of the discharge tube. This frequency conversion process is relatively high in efficiency so that, when it is combined with an efficient source of the exciting radiation, a high over-all lamp efficiency is produced.[1,2] Although a major portion of this paper is devoted to the gaseous conductor, a brief description of the characteristics of fluorescent powders is necessary in order to illustrate the require-

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ments which the gaseous conductor must meet.

Fluorescent Materials

Certain materials, known as phosphors,[3] have the property of emitting visible light when they are exposed to exciting agents such as cathode rays, X rays, and ultraviolet radiation. The light thus produced is called fluorescent light, and most engineers are familiar with this property as used in the fluorescent screen of the cathode-ray oscilloscope and the X-ray fluoroscope. However, by far the most efficient method of exciting these phosphors is the use of ultraviolet radiation, as in the process used in the fluorescent lamp.

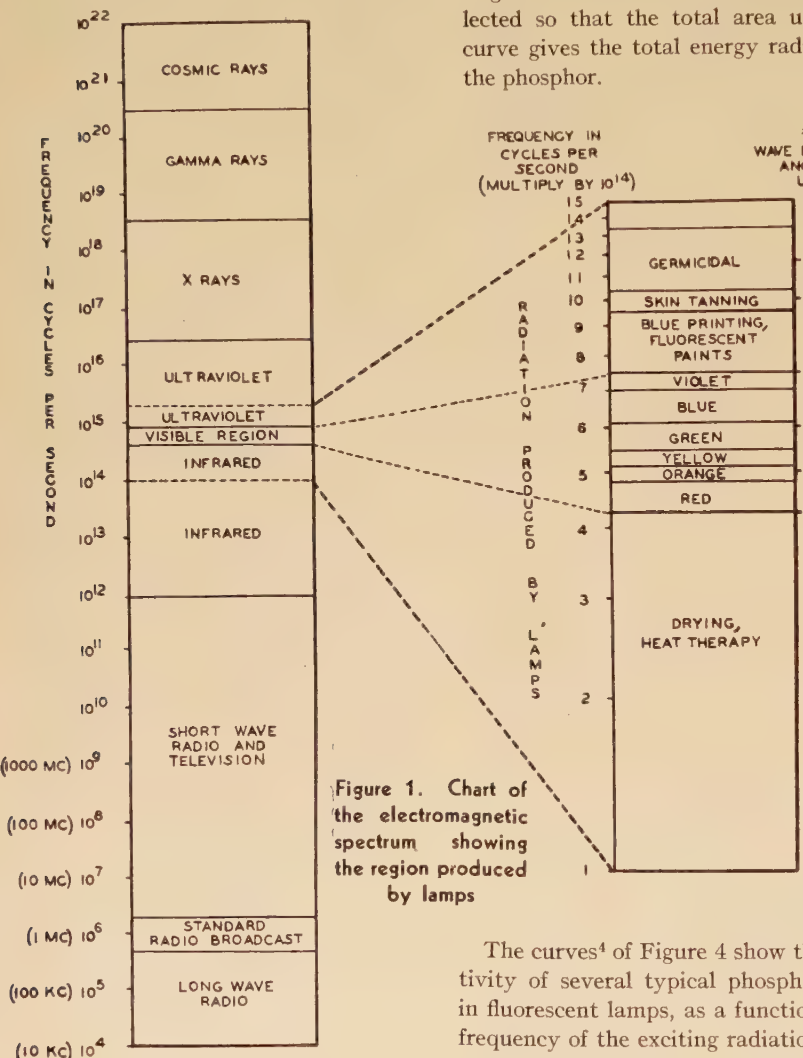

Figure 1. Chart of the electromagnetic spectrum showing the region produced by lamps

The color of the fluorescent light depends upon the type of phosphor and the methods by which it is prepared and is not, in general, influenced by the frequency of the exciting radiation. Table I lists some of the phosphors used in fluorescent lamps and gives the predominant color of the light which they emit. By properly mixing these different phosphors, one can obtain almost any desired color of light. The curves of Figure 3 show the distribution of the energy output with respect to frequency for daylight, blue, and pink fluorescent lamps. The continuous curves represent the energy radiated from the phosphor; the dotted rectangles represent the relatively small amount of visible energy produced by the mercury discharge.[4] It should be pointed out that the curves given in Figure 3 are plotted with respect to frequency, and they therefore differ in shape from curves plotted with a uniform wavelength scale. Ordinates have been selected so that the total area under the curve gives the total energy radiated by the phosphor.

The curves[4] of Figure 4 show the sensitivity of several typical phosphors used in fluorescent lamps, as a function of the frequency of the exciting radiation. It is seen that these have their maximum sensitivity in the region near $1{,}182 \times 10^{12}$ cycles per second (2,537 angstrom units), which is the radiation most efficiently produced by the low-pressure mercury discharge which makes up the gaseous conductor of the fluorescent lamp.

The Gaseous Conductor

A glass tube filled with neutral gas atoms, as, for example, an unexcited fluorescent lamp, is an almost perfect insulator. Even if electrons are freely introduced into the gas, only small currents can be made to flow with the usually available voltages, so long as the gas atoms remain electrically neutral. The reason for this is that the space charge produced by the electrons limits the flow of current. Only by breaking up the atoms of the gas into charged particles, or in other words, "ionizing" the gas, can reasonably large currents be made to flow. This process of ionization is essential to the operation of almost every type of gaseous conductor. In the case of a rectifier, thyratron, or similar device, the passage of current is the chief function of the discharge, and conditions are preferably arranged so that a minimum amount of energy is absorbed by the conductor. The primary purpose of the gas discharge lamp, on the other hand, is to produce radiation. As will be described later in the paper, radiation is obtained from "excited" atoms which have absorbed energy from the discharge. Hence, in the discharge lamp, conditions are preferably arranged so that as much energy as possible is absorbed by the conductor in the formation of excited atoms.

The gaseous conductor of the fluorescent lamp may be separated into three main regions, namely, the cathode region, the anode region, and the positive column. The most important of these is the positive column, which includes most of the length of the lamp, and in which most useful excitation (and hence radiation) is produced. The cathode and anode regions serve to connect the positive column to the external circuit. These are regions of low light-producing efficiency and, for this reason, conditions in general are chosen so that they consume as little as possible of the total lamp wattage. On alternating current the same electrode structure must serve alternately as anode and cathode, although the electronic phenomena are greatly different for the two functions. Before considering in detail these regions of the discharge, a brief description will be given of the fundamental processes of excitation and ionization.

Excitation and Ionization

Although there are many ways in which atoms may be excited and ionized, in a gas discharge lamp, we are primarily interested in excitation and ionization by electron collision. For the purposes of our description an atom may be pictured as a positively charged nucleus surrounded by a number of electrons. The mass of the nucleus is very heavy compared to that of an electron. In the normal state of the atom, the number of electrons is just

sufficient to neutralize the positive charge of the nucleus and in addition the internal energy of the atom is at its minimum value. If this normal atom is struck by an electron, the collision may be as between elastic spheres, and, due to its great mass, the atom will absorb very little energy from the electron. The small amount of energy which is absorbed in such a collision appears as a change in velocity of the atom (increase in thermal energy of the atom). If, however, the colliding electron possesses more than a certain minimum amount of energy, there exists a probability that the atom will make an inelastic collision and absorb a definite amount of energy from the electron. In this process one (in rare

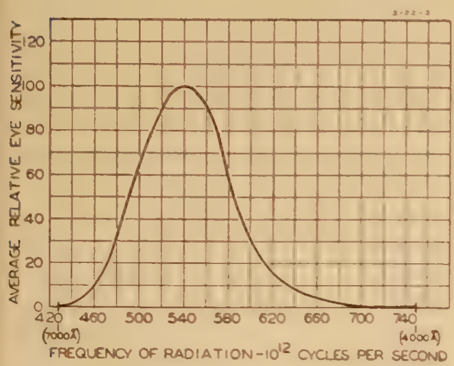

Figure 2. Average relative response of the light-adapted human eye as a function of frequency

cases more than one) of the electrons of the atom is changed from its normal state to a state of higher potential energy, and the atom is said to be "excited." In the new state the electron is more loosely attached to the nucleus but is still a part of the original atom; hence, an excited atom is still electrically neutral. For each different kind of gas atom certain characteristic discrete states of potential energy are possible, and these are usually called energy levels. An atom does not remain in an excited state indefinitely, but in most cases very quickly returns to the normal state and releases the energy of excitation in the form of radiant energy. The frequency of the radiation emitted during the transition from one level to a lower level depends upon the energy difference between the levels, and is given by the equation:

$$f = 242.9 \times 10^{14} V \text{ cycles per second} \quad (1)$$

where f is the frequency of the emitted radiation and V is the energy difference expressed in electron-volts (an electron-volt is the energy gained by an electron in traversing a potential difference of one volt). When several possible energy levels lie between a given excited state and the normal state, this transition may occur in several steps.

The lowest energy level (neglecting metastable states) from which a mercury atom can return to the normal state corresponds to an energy difference of 4.86 electron-volts. This transition, by equation 1, produces radiation at a frequency of $1{,}182 \times 10^{12}$ cycles per second, which is the radiation desired for exciting the phosphor in the fluorescent lamp.

Ionization is a process similar to excitation except that the energy absorbed by the atom in question is sufficient completely to remove one or more of its electrons. An atom which has lost an electron is positively charged and is known as a positive ion. Ionization may be produced in one step, if the colliding electron has energy greater than a certain value known as the minimum ionizing potential. This value for mercury is 10.38 electron volts. Other types of ionizing collisions exist, but there is evidence that they are

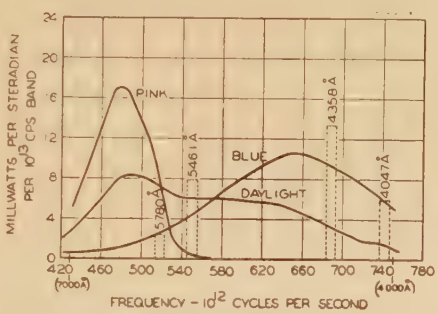

Figure 3. Spectral distribution of radiation from 15-watt fluorescent lamps of different colors

of lesser importance under the conditions of the fluorescent lamp.

The manner in which these fundamental processes of excitation and ionization are utilized in a fluorescent lamp will now be considered.

The Cathode Region

The cathode in a fluorescent lamp is of the type commonly known as oxide-coated (or Wehnelt) and consists of a coil of tungsten wire activated by means of a coating of alkaline-earth oxides such as barium and strontium oxides. During starting, the cathode is preheated, and for this purpose two external leads are provided to permit passage of the heating current directly through the cathode coil. After starting, the discharge provides sufficient heating of the cathode, and external heating is not required.

In normal operation only a small portion of the cathode area is heated by the discharge to an electron emitting temperature, and the available zero-field emission current is considerably less than the total lamp current. Under these conditions a positive-ion space charge builds up at the cathode and produces an electric field at the cathode surface. The region of high electric field is called the cathode sheath. This field very greatly increases the electron emission in a manner which is characteristic of activated surfaces.[5] The generally accepted picture of the cathode phenomena under these conditions is as follows.[6]

Electrons emitted by the cathode are accelerated by the field which exists in the above-mentioned positive-ion cathode sheath. The voltage drop across this region is known as the cathode fall and has a value slightly larger than the minimum ionizing potential of the gas which surrounds the cathode. The electrons from the cathode thus acquire energies capable of ionizing and exciting the atoms with which they collide in the region beyond the positive ion sheath. The positive ions produced in this region diffuse in all directions, and those moving toward the cathode counteract the space charge of the electrons and create the field of the cathode as described above. Because the positive ions have masses

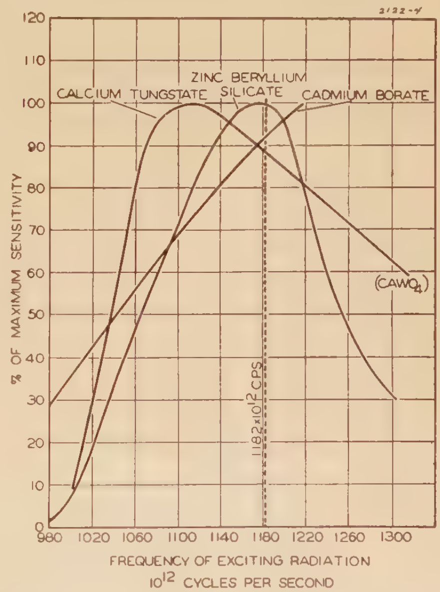

Figure 4. Relative sensitivity of typical phosphors used in fluorescent lamps, as a function of frequency of exciting radiation

many times greater than the electrons, they move much more slowly, and therefore relatively few are required to counteract the space charge of the electrons. In the case of mercury, the relative velocities of an electron and a positive ion, both having the same energies, are approximately 600 to 1.

An approximate calculation of the

zero-field emission current under actual lamp-operation conditions will serve to illustrate the order of magnitude of the quantities involved. For an eight-watt lamp cathode operating at 0.18 ampere, observed values of emitting area and temperature are approximately 0.01 square centimeters and 1,180 degrees Kelvin, respectively. Using the data given by Dushman,[7] the zero-field emission current is calculated to be 0.75 ampere per square centimeter, giving a total current of 0.007 ampere. Thus, for the particular conditions here described, the zero-field emission is only about four per cent of the total lamp current. This figure agrees in order of magnitude with the value of ten per cent given by Found[6] for a similar type of cathode. That the observed current is so much greater than this zero-field value is explained by the above-mentioned action of the electric field at the cathode. In addition a small fraction of the current

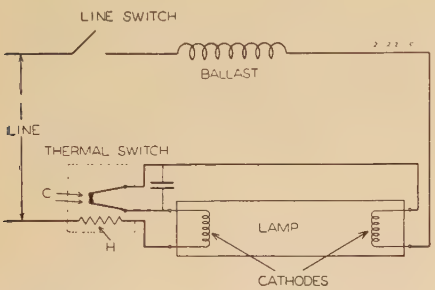

Figure 5. One of the common automatic starting circuits for fluorescent lamps

is carried by the positive ions which move toward the cathode.

The electrons from the cathode, after producing ions in the region beyond the cathode sheath, have relatively low energies. By some mechanism not well understood, these electrons acquire a random distribution of velocities corresponding to a Maxwellian distribution. (When velocities are distributed in this manner, they may be represented by an equivalent temperature T_e, just as the velocities of the atoms of a gas are represented by the gas temperature. Thus we can speak of an electron temperature.) These low-energy electrons and the positive ions both diffuse from the cathode glow region and after a short distance enter a region where the voltage gradient begins to rise. This is where the positive column begins.

The Positive Column—Conduction Processes

The positive column is a region characterized by a uniform voltage gradient and a uniform degree of ionization throughout its length. Electrons entering from the cathode region gain energy from the gradient along the tube but nevertheless maintain a Maxwellian distribution of velocities, which corresponds to a temperature greater than that of the entering electrons, and which is many times greater than the temperature of the gas. These electrons drift toward the anode under the influence of the longitudinal gradient and, because they move faster than the positive ions, constitute all but a negligible fraction of the discharge current. This drift velocity of the electrons is small compared with their random velocities. In moving through the gas, these electrons collide with atoms of the gas, and some of these collisions result in the formation of positive ions and additional electrons. The electrons and positive ions have essentially equal concentrations in the positive column, and both diffuse toward the wall. As a result of their smaller mass, the electrons have higher velocities and tend to diffuse faster than the positive ions. This difference in rate of diffusion produces a radial electric field which accelerates the motion of positive ions and slows up the motion of electrons to the wall, until the two rates are just equal. This condition is necessary, because obviously the net current to the wall must be zero. It is apparent that the greater the electron velocities, the greater must be this radial field, and hence the greater must be the rate at which positive ions are lost to the wall. Tonks and Langmuir[8] have developed a relation called the plasma balance equation which expresses the rate at which ions are lost to the tube wall as a function of the electron temperature.

Not only is the rate at which ions are lost to the tube wall dependent on electron temperature, but also the rate at which they are generated is a function of the electron temperature. This follows from the fact that the probability that an electron will produce ionization by collision increases with the energy of the electron in excess of the minimum ionizing energy. (For very high energy electrons this probability decreases again.) This probability can be measured and is known for mercury vapor.[5] Thus it is possible to calculate the rate at which ions will be produced in a gas by electrons having any given electron temperature.[9]

In the steady state, for the type of discharge used in the fluorescent lamp, the rate at which ions are produced must equal the rate at which they are lost to the tube wall. Therefore the equations expressing the rate of ion generation and the rate of ion loss can be equated as shown by Found.[10]

$$(2V_T + V_i)e^{-V_i/V_T} = \frac{0.9\, s_o}{B\, a} \cdot \frac{T_g}{P_g} \cdot \left(\frac{m_e}{m_p}\right)^{1/2} \qquad (2)$$

where V_T is the voltage equivalent of the electron temperature, V_i the minimum ionizing potential of the gas, s_o is a factor dependent on the geometry of the container and the gas pressure, a is the tube radius, B is a constant relating to the probability of ionization which can be measured for any given kind of gas, T_g and P_g are respectively the temperature and pressure of the gas within the tube, and m_e and m_p are respectively the mass of an electron and the mass of a positive ion of the gas. From this equation it is possible to calculate the electron temperature for a tube containing a given gas at a known pressure by means of known constants. It can be seen that for any given gas the electron temperature (electron energy) decreases with an increase in tube radius and gas density and that the electron temperature remains constant as long as the product of gas density and tube radius remains constant.

This equation does not describe the experimental fact that the tube gradient decreases with an increase in arc current. This property of the positive column of

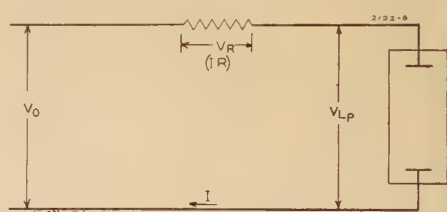

Figure 6. Simple resistively ballasted operating circuit

the fluorescent lamp is accounted for partly by an increase in mercury vapor pressure with an increase in input watts. In addition, Tonks and Langmuir[8] propose the explanation that two-stage ionization processes would permit lower energy electrons to produce ions and would therefore require a lower electron temperature than is needed for single-stage ionization conditions.

As was stated above, the positive ions are produced in the positive column only by the higher-velocity electrons in the Maxwellian distribution. This fact is of great importance in the production of radiation from a gas discharge lamp.

The Positive Column—Radiation Generation

In order to obtain a sufficient number of high-velocity electrons to satisfy the need for positive ions in the positive column, it is necessary (because of the Maxwellian distribution of velocities) to create a much larger number of lower-velocity

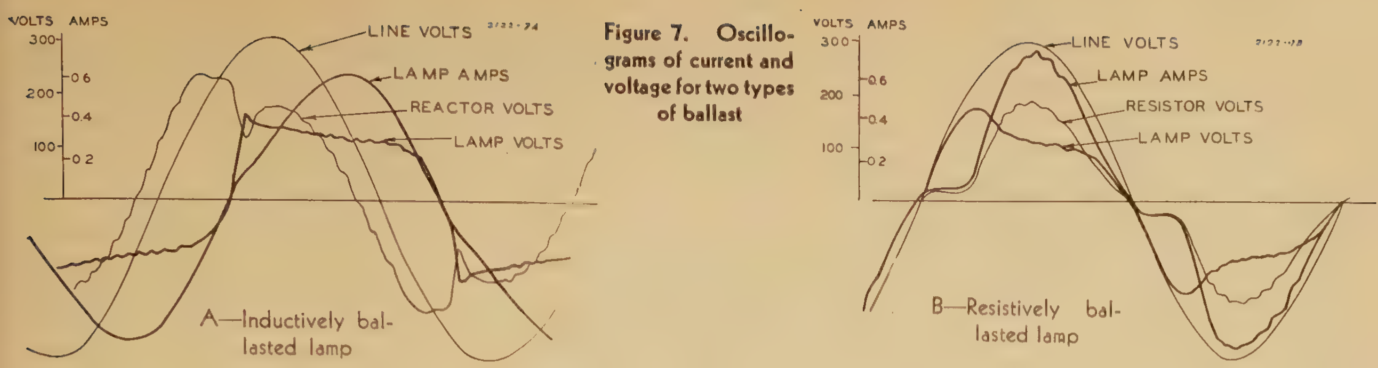

Figure 7. Oscillograms of current and voltage for two types of ballast

electrons. For example, if the average energy of the electrons is two electron volts, only 1.5 per cent of the total number have energies above the minimum ionizing potential of mercury, while 18 per cent have energies greater than the minimum excitation potential of mercury. Thus the process of radiation generation is only indirectly the result of the passage of current through the discharge tube. It follows that in order to increase the amount of radiation at any given frequency it is necessary to adjust the conduction processes so that the resulting electron energy approaches an optimum value for exciting the gas atoms to the desired energy level. In the practical lamp ideal conditions cannot be reached, because of considerations regarding total light output for a given size of lamp and other factors which cannot be considered here, relating to the lamp as a light source rather than as an electronic device.

The Anode Region

The anode region serves to connect the positive end of the positive column to the external circuit. Conditions at the anode are similar to those at the tube wall, except that the electron current reaching the anode must exceed the positive ion current by an amount equal to the current through the tube. If the anode area is large enough, so that the rate at which the electrons strike it as a result of their random motions is equal to the discharge current, no anode drop is necessary. If the anode area is larger than this, it develops a negative voltage with respect to the gas and repels electrons, until the net current reaching it just equals the tube current. If the anode area is too small, it develops a positive voltage with respect to the gas and attracts electrons until the net current collected is equal to the drift current. In this case a sheath builds up around the anode, until the electron current entering the sheath is equal to the discharge current. The area of this sheath is then the effective anode area. By convention, a positive anode drop means an increase in tube volts. In the fluorescent lamp, the anode area is such that the average anode drop has a positive value of approximately seven volts. This value is primarily determined by the requirements of cathode heating.

Electrode Heating

No mention has been made of the manner in which cathode is heated, except to state that this is done by the discharge. This process will now be discussed more in detail. On alternating current, each electrode functions alternately as anode and as cathode. During both half cycles, heat is removed by conduction and radiation, and a small amount of heat is produced by the passage of current through the cathode coil. During the cathode half cycle an additional loss of heat occurs as a result of the evaporation of electrons from the cathode, and at the same time heat is gained from the positive ions which reach the cathode. On the anode half cycle heat is supplied by the electrons, which are accelerated through the voltage drop in the anode sheath and deliver this energy—in addition to their thermal energy and the heat of condensation—to the electrode. This is the most important source of electrode heating in a-c operation. The total net heating is the average of that produced on the two half cycles, and in the usual fluorescent lamp this is more than sufficient to raise the cathode to an operating temperature. In fact, it is usually necessary to attach projections to the electrode to reduce the amount of heat gained on the anode half cycle, in order to prevent evaporation of the cathode material.

Over-All Characteristics of the Fluorescent Lamp

Like most electronic devices the operation of the fluorescent lamp is greatly influenced by the characteristics of the circuit to which it is connected. Consequently, any description of lamp characteristics must be made with reference to a particular set of circuit conditions.

For reasons to be described in this section, the fluorescent lamp cannot be satisfactorily started and operated unless the line voltage is greater than the lamp voltage. Consequently a voltage-absorbing device must be connected in series with the lamp to absorb the difference between the two. This necessary difference between lamp voltage and line voltage is the determining factor in selecting the amount of ballast required. Commercially this ballast consists of a resistance for d-c operation, while on alternating current it may be a resistance, an inductance, or a combination of inductance and capacitance. The commonly made statement that a ballast is required because of the negative volt-ampere characteristic of the lamp is true, but there are other equally important reasons for its use. These reasons are, as will now be described, the need for lamp starting, the need for good regulation with changes in line voltage, and the need for good current wave shape on a-c circuits.

Lamp Starting

In order to start a fluorescent lamp, it is necessary to ionize the gas between the electrodes and to heat the cathodes to an electron-emitting temperature. Perhaps the simplest method of accomplishing this is to apply a voltage several hundred volts in excess of the normal lamp-operating voltage across the lamp terminals. With this method of starting, however, the cathodes are required to emit electrons before the discharge has time to heat them to an emitting temperature, and the resulting high cathode drop may produce destructive sputtering of active material from the cathode surface. Also a disadvantage of this method of starting is the fact that the size and wattage loss of the ballast are greater than would otherwise be required. Conventional starting circuits avoid these difficulties by the use of a switch which connects the cathodes in series with the ballast, so that they are adequately preheated, and then opens the circuit to provide an inductive surge to start the lamp. The circuit of Figure 5 shows a thermal switch arrangement used

for automatic starting of the 100-watt fluorescent lamp. Contacts are mounted on bimetallic elements, so that they can be opened by heat produced in heater H. When the main line switch is closed, the short-circuit current of the ballast preheats the cathodes. Heater H is connected in series, and after time has been allowed for the cathodes to be heated, contacts C-C are caused to open. The resulting inductive surge starts the lamp, and lamp current then flows through heater H, keeping the contacts open. This starting action may also be accomplished by means of a glow switch.[11] In the case of direct current, if the ballast consists of a resistor, no inductive surge is produced when the switch opens. This difficulty is overcome by using a series inductance which does not contribute to the ballasting, but which supplies the necessary inductive starting surge.

Operating Stability

On a perfectly regulated line, a fluorescent lamp would theoretically require a ballast just sufficient to offset the negative volt-ampere characteristic of the arc, and the lamp voltage could be made nearly equal to line voltage. In a practical circuit, however, the ballast must be greater than this value, in order to take care of normal variations in line voltage.

This fact can be demonstrated by the following approximate analysis of a resistively ballasted lamp. Over the normal range of operating currents, the lamp voltage changes by only a small amount and consequently can be treated as a constant. Referring to the circuit of Figure 6

$V_o = V_{Lp} + IR$ and IV_{Lp} = lamp watts $= W_{Lp}$

$dV_o = RdI$ and $dW_{Lp} = V_{Lp}dI$

Expressing the change in watts as a fraction of the total lamp watts

$$\frac{dW_{Lp}}{W_{Lp}} = \frac{V_{Lp}dI}{V_{Lp}I} = \frac{dI}{I} = \frac{dV_o}{IR}$$

$$= \frac{dV_o}{V_o} \cdot \frac{V_o}{IR} = \frac{dV_o}{V_o} \cdot \frac{V_o}{(V_o - V_{Lp})}$$

From the above equations we see that, if the lamp voltage were half the line voltage five per cent change in line voltage would produce a ten per cent change in lamp wattage, while if the lamp voltage were two-thirds the line voltage, a five per cent change in line voltage would produce a 15 per cent change in lamp wattage. How nearly the lamp voltage may be made to approach line voltage is limited by the permissible variation in lamp watts and by the expected variation in line voltage.

With an inductive ballast a similar analysis gives the relation

$$\frac{dW_{Lp}}{W_{Lp}} = \frac{dV_o}{V_o} \cdot \frac{V_o^2}{(V_o^2 - V_{Lp}^2)}$$

so that on this type of ballast the lamp voltage may be made to approach more nearly the line voltage for the same regulation than is the case with resistive ballast. In practice the wattage changes are greater than shown here, because the lamp voltage decreases with an increase in current. For most commercial ballasts the ratio of line volts to lamp volts is approximately 2 to 1.

Table I. Phosphors Commonly Used in Fluorescent Lamps[4]

Phosphor	Frequency, in 10^{12} Cycles Per Second of Maximum Radiation Output	Wavelength, in Angstrom Units, of Maximum Radiation Output	General Color
Calcium tungstate	680	4,400	Blue
Magnesium tungstate	625	4,800	Blue-white
Zinc silicate	570	5,250	Green
Zinc beryllium silicate	505	5,950	Yellow-white
Cadmium borate	484	6,150	Red

In addition to resistive and inductive ballast, a third type of ballast is used in commercial circuits. This consists of a combination of an inductance and a capacitance connected in series. This type of circuit has a more nearly constant current characteristic, and it is possible to operate the lamp more nearly equal to line voltage. Such circuits are usually used in conjunction with inductive ballasts, and the regulation of the inductive ballast controls the choice of line voltage as explained above.

Wave Shape

On alternating current it is necessary also to consider the requirements of wave shape of current and voltage. This results from the fact that on alternating current the lamp must be reignited on each succeeding half cycle. The type of ballast has an important influence upon the wave shape, as is illustrated by the oscillograms of Figure 7. In both oscillograms the rms values of line voltage and lamp current were set at 210 volts and 0.42 ampere respectively. For the inductively ballasted lamp shown in Figure 7A, it is seen that at the instant of current zero the line voltage has a value of approximately 250 volts, while the lamp requires only 150 volts for reignition. The difference between these two is absorbed by the reactor. For the resistively ballasted lamp of Figure 7B the lamp current is in-phase with the voltage, and consequently zero voltage is available for reigniting the arc at the instant of current zero. The current therefore remains at a low value for a period of approximately 20 degrees before reignition takes place. Due to this long wait, deionization of the positive column has begun to take place, so that 175 volts are required to reignite the arc.

A similar lag in reignition may occur on inductive ballast, if the line voltage is not sufficiently greater than the lamp voltage. The type of current wave shape shown in Figure 7B is not so desirable as that of Figure 7A, because of increased flicker, or stroboscopic effect, due to the extended period of zero current.

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Regulated Rectifiers in Telephone Offices

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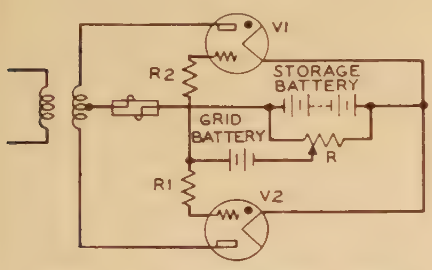
FOR many years rectifiers of the garage type were used in converting alternating current to direct current for charging batteries used for communication purposes. These batteries furnish power for relay operation, for talking, and filament and plate supplies for repeaters. The rectifiers were of the manual-control type where the operator selected the charging current by means of tap switches or rheostats.

With the development of the thyratron type of tube, a rectifying means was made available in which the grid of the rectifier tube could be used to control its own output current by an electronic circuit. Rectifier circuits were designed to maintain a constant output voltage. If a regulated rectifier is connected to a battery and the constant rectifier voltage is 2.15 volts per cell, the load current will automatically come from the rectifier and not from the battery. Also the battery will draw from the rectifier sufficient additional current to maintain its charge. If the circuit voltage is held within limits of less than plus or minus one per cent, the maintenance of the battery is reduced and its life is extended.



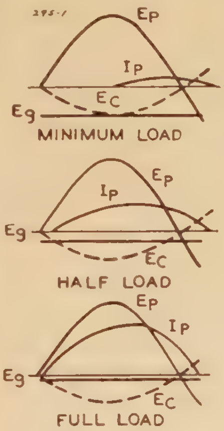


Figure 1. Magnitude control circuit

The thyratron tube differs from the vacuum tube in that the grid does not have a continuous control of the plate current.[1] When a positive potential is applied to the plate, current does not flow until the magnitude of the negative grid voltage is reduced to the critical value, at which time the plate current flows, and the magnitude of the plate current depends upon the voltages and impedances in the circuit. The grid has no further control, and plate current flows until it is stopped by reducing the plate voltage to zero.

Thyratron tubes use various gases and mixtures of gases. The earliest type used mercury vapor, but this type of tube is quite sensitive to temperature changes. The grid characteristics are shifted materially by changes in the room temperature in which it is operated, and in low temperatures it is almost a vacuum tube. Thyratron tubes using argon gas are not affected by temperature changes, but high-pressure argon tubes have a low inverse voltage which limits their application to low-voltage rectifiers. Tubes using low-pressure argon have a higher inverse voltage, but are accompanied by a high arc drop which makes their efficiency low. A mixture[2] of mercury vapor and argon has been found which provides the temperature-stable grid characteristic of the argon tube and the low arc drop of the mercury-vapor tube. This type of tube has been very successful with certain regulating circuits, particularly at voltages less than 60 volts.

Five kinds of regulating circuits are used in telephone offices to hold the output voltage of rectifiers constant. The selection of the circuit to be used depends upon the magnitude of the current, d-c voltage, and type of rectifying means to be used. Two forms of regulating circuits using thyratron tubes and one using two-element high-pressure tubes were developed. A fourth circuit using all vacuum tubes was adapted for telephone use. The fifth kind uses a negative resistance.

Magnitude Control

The automatic regulating circuit using the temperature-stable tubes has been called the magnitude method[3] of control, since the voltage applied to the grid is a d-c voltage, and its magnitude is varied to control the plate current of the tube. Figure 1 shows a schematic circuit and diagrams of the voltages applied to the rectifier tube. The rectifier circuit is the conventional full-wave circuit with a filter coil in the negative output lead. A negative voltage, with respect to the tube cathodes, is taken off a potentiometer connected across the battery to be floated. This negative voltage is opposed by a slightly smaller positive voltage of a grid battery. The net negative voltage of a few volts is applied to the grids of the tubes through grid resistances. The top diagram in Figure 1 shows the positive half of the a-c voltage applied to the tube. The dotted line represents the critical grid voltage of the tube, I_p the plate current that flows, and E_g the d-c grid voltage applied to the grids. If the applied grid voltage is more negative than the critical value, the tube does not fire and no plate current flows. If the magnitude of the grid voltage is decreased until it reaches the critical value, the tube fires and the rectifier delivers output. A further decrease in the grid voltage will result in the critical value being equaled earlier in the cycle, and the average value of the plate current will increase. Finally, with the grid voltage zero, the tube fires at the beginning of the cycle and the maximum average value of current is obtained.

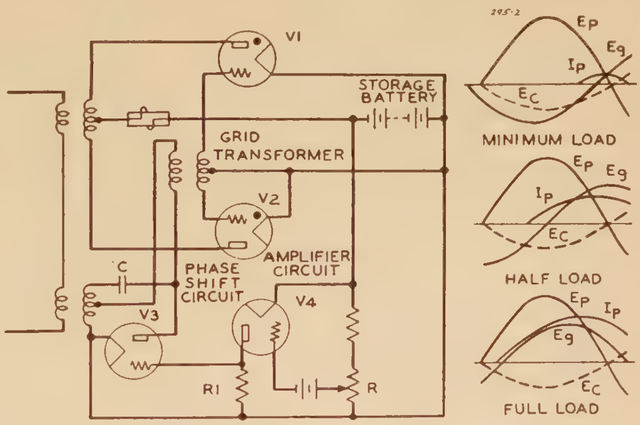


Figure 2. Phase-shift control circuit

If this circuit is operating without a filter coil, the current could be reduced by the variable grid voltage to only one half its maximum value, and then it would drop to zero due to its having control only during the first 90 electrical degrees. A smooth control from full load to light load can be secured by using a filter coil

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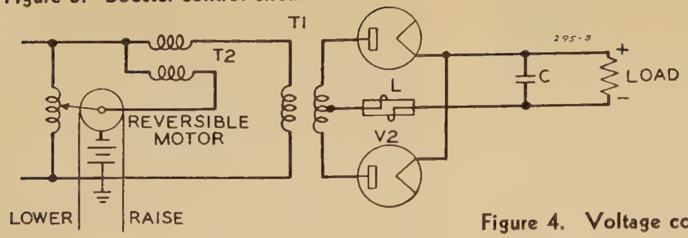
Figure 3. Booster control circuit



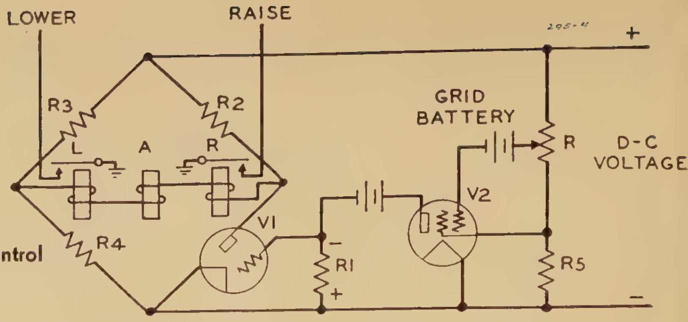


Figure 4. Voltage control for booster circuit

that has a high inductance with small currents, so that even though the tube fires near 90 degrees the current that flows is limited by the reactance of the coil. At full load its inductance is reduced by saturation of the iron to the value required for filtering. With conventional coil design where the no-load inductance can be made approximately five times the full-load inductance, the rectifier output current can be reduced by grid control to five per cent of the full-load value. When a telephone office load is applied to the battery, its external voltage decreases a fraction of a volt. This change is reflected over the potentiometer and grid battery to the grids to reduce the grid bias and increase the plate current of the tubes sufficiently to carry the office load.

With this circuit the battery voltage is held to a change of about one volt from no load to full load. This change in battery voltage is approximately the same for all values of battery voltage, as the grid must be varied about the same amount. Thus a better per cent regulation is obtained at the higher battery voltages. The regulating circuit will compensate for slight changes in the grid characteristics of the tubes. However, the large changes encountered in the case of mercury tubes make them unfit for this type of circuit. The mixture of mercury and argon has been found quite satisfactory when used in rectifiers covering a range of 24 to 60 volts. These tubes, however, have a low inverse-voltage rating and cannot be used in charging the higher-voltage storage batteries. The mercury tube has the necessary high inverse-voltage rating, so that a circuit was developed which has been called the phase-shift type of control; this enables the mercury tubes to be used, in spite of their changes of grid characteristic with temperature.

Phase-Shift Control

Figure 2 shows a schematic diagram of the phase-shift type[4] of control circuit. This circuit differs primarily from the magnitude circuit in that the grid voltage is an a-c voltage of a fixed magnitude, but its phase relation with respect to the a-c plate voltage is varied to control the output of the rectifier. When the grid voltage is out of phase with the plate voltage, the grid is negative all during the cycle that the plate is positive, so that the tube does not fire. As the grid voltage is shifted in a direction to be inphase with the plate voltage, the instantaneous grid voltage reaches a magnitude which is less negative than the critical value so that the tube will fire and deliver current for the remainder of that cycle. When the grid voltage is shifted to be practically inphase with the plate voltage, the tube fires at the beginning of the cycle and carries current during the remainder of that cycle. In this way the output current of the rectifier is controlled by shifting the phase from the out-of-phase to the inphase condition.

The grid voltage of varying phase is obtained from the phase-shift circuit which consists of a bridge circuit with two windings of the transformer furnishing two arms of the bridge, a capacitor the third, and the plate-cathode resistance of a vacuum tube the fourth. A transformer is connected across the galvanometer corners of the bridge which supplies the voltage to the grids of the thyratron tubes. When the plate-cathode resistance of the vacuum tube is at its minimum value, the grid voltage is nearly inphase with the plate voltage, the output current to the rectifier being maximum. When the vacuum tube is biased out (providing an infinite resistance) the grid voltage is practically out of phase with the plate voltage.

The plate-cathode resistance of the vacuum tube is controlled by the d-c bias applied to the tube. This varying grid bias of 2 to 20 volts is obtained from a vacuum-tube d-c amplifier circuit. When a telephone-office load is applied to the storage battery it decreases the battery voltage a fraction of a volt. This small voltage is amplified by the d-c vacuum-tube amplifier and applied to the phase-shift vacuum tube in a direction to shift the grid voltage more nearly into phase and thus increase the output of the rectifier. In this way the storage-battery voltage is held to the regulated value. The accuracy of regulation depends on the amount of gain in the d-c amplifier circuit. This circuit has been built to provide regulation in the order of a few hundredths of a volt, but in most applications one per cent regulation is all that is required. The filter coil is not essential for the operation of this circuit, but a swinging choke whose inductance changes four to one will assist in regulation.

In a thyratron with an oxide-coated filament, operating filament temperature must be reached before output current can be drawn from the rectifier. An inexpensive way to prevent output current is to apply a large negative grid bias to the tubes, which biases them out even though the a-c voltage is applied to the plates. With the magnitude method, the grid circuit can be switched to the battery voltage and in the phase-shift circuit it is only necessary to open the resistance arm of the phase-shifting circuit. This switching can be done with a small telephone-type relay on all sizes of rectifiers. The time delay is provided by a conventional bimetallic-strip type of relay operating the telephone relay.

The cost of the thyratron tube, in sizes above ten amperes per tube, is large in comparison with the cost of the complete rectifier. High-gas-pressure rectifying tubes can be operated with the tube elements at high temperatures if the grid is omitted. This permits a two-element tube to be built at about one fifth the cost of the thyratron, thus making it practical to build low-voltage rectifiers in sizes of 30 to 100 amperes.

Booster Control

In order to regulate the output of a rectifier using two-element tubes, a circuit has been developed which has been called the booster type of control. Figure 3 shows a schematic diagram of this type of circuit. The output current of the tubes is controlled by changing the plate voltage applied to the tubes. The plate voltage is varied by means of a circuit consisting of a continuously tapped auto-transformer which changes the voltage applied to the primary of a booster transformer. When the continuously tapped

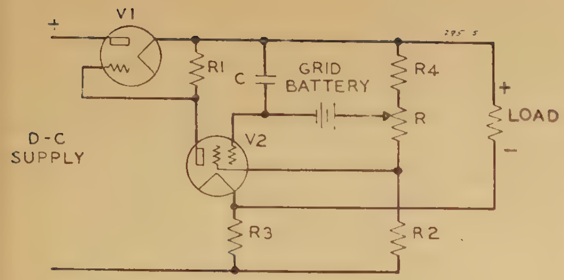
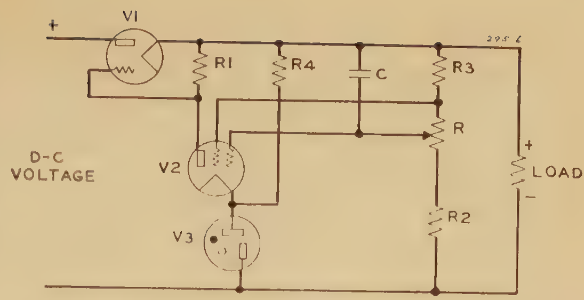


Figure 5 (left). Series-tube control with grid-battery voltage standard

Figure 6 (right). Series-tube control with cold-cathode tube as voltage standard



autotransformer is at its zero end, no voltage is applied to the booster transformer and the ratio of the plate transformer is selected so that the rectifier delivers minimum current. The output current of the rectifier is increased by driving the continuously tapped autotransformer to its maximum boost position which raises the plate voltage until the rectifier delivers its maximum output current. The continuously tapped autotransformer is controlled by means of a small reversible motor so that closing the circuit of the "raise" lead will increase the rectifier output current and closing the circuit of the "lower" lead will drive the motor in the direction to decrease the rectifier output current. The disadvantage of this circuit is that a second transformer 40 per cent as big as the plate transformer is required in addition to the autotransformer. The advantage is that all of the equipment in this circuit operates at a high efficiency, and with the boost type of connection the no-load loss is small. Even the core loss of T_1 is reduced, since it operates at a lowered voltage at light loads. Light-load efficiency is important in telephone applications, as the rectifier must operate many hours at night with practically no load. The input power factor is the same as that of the unregulated tube-rectifier circuit and is not reduced materially by the control circuit.

Various combinations of this rectifier circuit are possible in that the plate voltage, in small sizes of rectifiers, may be connected directly to the continuously tapped autotransformer or the auxiliary transformer may be connected to buck the line voltage. This circuit can be applied to more than one phase with the appropriate tapped autotransformers, booster transformers, and plate transformer to control each phase.

A voltage controller to operate with the booster type of regulating circuit is shown in Figure 4. This circuit is to translate variations in the magnitude of the d-c voltage to be regulated into a raise and lower signal to the reversible motor in the rectifier. The circuit consists of a d-c amplifier to increase the small changes in the load voltage from a fraction of a volt to several volts. This amplified voltage is applied to the grid of a vacuum tube which constitutes one arm of a resistance bridge circuit. Across the galvanometer corners of the bridge are connected two polarized relays in series with a nonpolarized relay.

When the load voltage is at the correct value, the bridge is balanced and no voltage appears across the galvanometer corners of the bridge, so that all relays are released. The change in the load voltage will be reflected through the amplifier and bridge circuit in a direction to unbalance the bridge and cause either the R or L relay to operate, depending on which way the voltage changed, which in turn operates the motor in the rectifier circuit. Plate supply for the bridge tube V_1 is obtained from the d-c voltage being regulated, but a separate source of direct current is required for the plate of the amplifier tube V_2. This may be a battery or a small half-wave disk rectifier with a filter capacitor to provide 50 volts, one milliampere for the plate of V_2. The standard voltage can be either a grid battery or the negative resistance regulating circuit described below. The nonpolarized relay A operates on twice the current of the R and L relays, and is connected in series with them to give an indication when the regulated voltage is approximately twice the variation that causes the R and L relays to operate. In this way, an alarm may be provided to indicate when the regulated voltage goes out of the regulating range.

The accuracy of this control is entirely a function of the amount of d-c amplification provided by tube V_2. A single-tube amplifier operating with a load voltage of 24 or 48 volts will provide regulation in the order of 0.1 to 0.2 volt. An ordinary voltage relay may be used instead of this circuit, but its accuracy is limited by the difficulty of adjusting the span between the high and low contact of the relay to less than one volt. An additional advantage of this circuit over the voltage relay

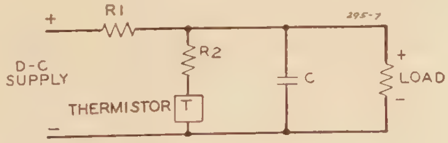


Figure 7. Shunt thermistor circuit

is that the span between the "lower" and "raise" signal is a value fixed by the design of the circuit, but the average value of the regulated voltage may be shifted simply by adjusting the rheostat R.

Series Tube Control

Where a small amount of closely regulated power is required, the series vacuum-tube type of regulated rectifier is used.[5] The conventional full-wave vacuum-tube rectifier and filter is used to provide about 100 volts more than the regulated value. A vacuum tube is connected in series with the output and its plate-cathode resistance acts as a dropping resistance. The amount of resistance it introduces into the circuit depends upon the magnitude of the grid voltage. To reduce the rectifier d-c voltage automatically to the regulated value the circuit shown in Figure 5 is used. The output voltage is applied by means of the voltage drop over the potentiometer R, which is opposed to a constant-voltage source such as a grid battery to the grid of V_2. If the grid-battery voltage is of the same magnitude as the load voltage, the maximum accuracy of regulation is obtained. However, with a screen grid or pentode tube sufficient amplification is obtained with lower-voltage grid batteries. By adding the R_3 resistance which is in series with the load current, its voltage drop is applied to the grid of V_2 to compensate for load changes.

With a circuit of this type, plus or minus ten per cent variations of d-c supply can be reduced to the order of 0.05 volt

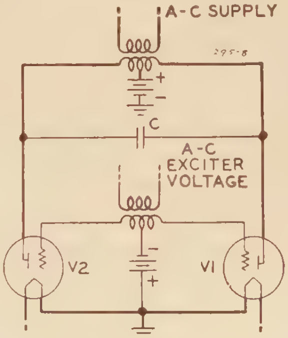


Figure 8. Fundamental inverter circuit

with constant load and to about 0.1 volt with variations in load. Better regulation than this can be obtained by carefully selecting the constants of the circuit or by adding a second stage to the d-c amplifier. However, with regulation of less than 0.05 volt, the contact potential in the grids of the d-c amplifier tube becomes a factor which causes random variations due to temperature changes in the cathode, produced mainly by variations in the heater voltage.

The circuit shown in Figure 6 is a variation of that shown in Figure 5 in that the voltage drop over a cold-cathode gas-filled tube is used as the standard for voltage regulation where accuracy of plus or minus one per cent to plus or minus three per cent is permissible. The arc drop of the tube V_3 is connected in the cathode circuit of the d-c amplifier circuit, and its grid voltage is picked off the potentiometer circuit, consisting of resistances R_2, R, and R_3, at a point which makes the grid of V_2 slightly negative with respect to its cathode. A variation in the load voltage in this way varies the grid of the amplifier tube V_2 whose amplified output appears as a voltage drop across R_1, to control the grid of the series tube V_1. It is desirable to add the R_4 resistance so that the V_3 tube can be operated at its most efficient current value, and the value of the R_1 resistance should be quite large so that the cathode current is small in proportion to the tube current in V_3.

In both of the circuits shown in Figures 5 and 6, feed-back filtering is obtained because the regulating circuit compensates for variations in voltage due to the ripple on the d-c supply in the same manner that it compensates for variations due to load. If the grid is not connected directly to the positive side of the load, the capacitor C should be added to conduct the ripple directly to the grid. Otherwise, a loss in ripple voltage would be obtained in proportion to the potentiometer ratio.

In this type of circuit the amplifier tube V_2 is usually of the heater type so that during starting, or in the event of a failure of the V_2 tube, the output voltage is unregulated and reaches a value approaching that of the d-c supply, except for the zero grid drop of the series tube V_1. This may be reduced by connecting a cold-cathode tube from the plate of V_2 to the potentiometer circuit at a voltage point with respect to the positive output terminal, which is slightly less than the breakdown voltage of the tube. Then with the circuit in normal condition, the tube will not pick up and will be an open circuit. If, however, the output voltage increases above the regulated value, the tube picks up and draws a current through the R_1 resistor which biases the series tube negative, reducing the output voltage. As soon as the V_2 tube is operating normally, the voltage of its plate terminal is drawn toward the negative end of the d-c output sufficiently to cause the cold-cathode tube to drop out. In this way, the load may be protected from an excessive voltage while starting or when trouble occurs in the amplifier tube.

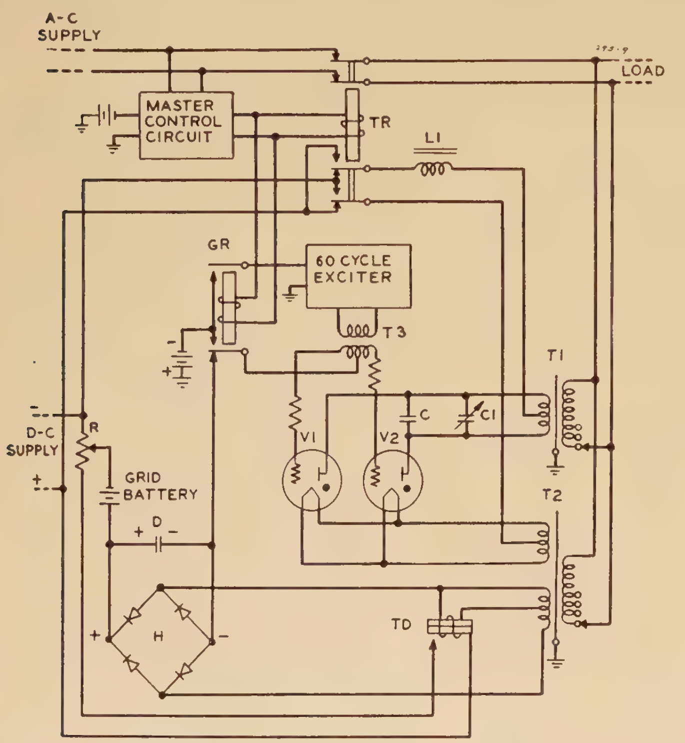


Figure 9. Combined rectifier–inverter circuit

Shunt-"Thermistor" Control

In regulated rectifiers where the use of a cold-cathode tube as a voltage standard does not provide sufficiently accurate regulation, a miniature regulated rectifier is used in place of the grid battery, where its higher cost is justified. The regulating part of the circuit is shown in Figure 7. The negative resistance is a "thermistor" made of a semiconductor. The value of R_2 is selected so that a change in d-c supply voltage causes a positive voltage drop in R_2 equal to the negative voltage drop in the thermistor with the net result that the voltage across the load is approximately constant. A resistance R_1 must be connected in series with the d-c supply to take up the variations in d-c supply voltage. This circuit, in general, is adaptable to voltages less than 100 volts and currents of a few milliamperes. Since most thermistors are sluggish in operation, a large capacitor C is connected across the circuit to smooth out rapid variations in d-c supply. The circuit is compensated for ambient temperature variations, as the resistance of thermistors is affected by temperature. This circuit has been used as a grid supply for amplifiers.

Rectifier–Inverter

With the increase in use of a-c voltages for filament and plate supply for repeaters, it became necessary to provide some means of providing power to operate these circuits when the commercial power supply had failed.[6] The electronic inverter using thyratron tubes provides a means to take energy from a storage battery and convert it into 60-cycle alternating current to keep the circuits functioning until the commercial power is restored. The elements of an electronic inverter are essentially the same as a rectifier, so that with the addition of a pilot source of 60 cycles and suitable switching means, one unit can serve as a regulated rectifier to float the storage battery normally, and during the time that the power has failed the circuit can take power from the battery and deliver 60-cycle power for use in the telephone office.

Figure 8 shows a schematic diagram of the fundamental inverter circuit. The battery is applied to the center tap of the plate transformer, and the grids of the tubes are held negative with a d-c bias sufficient to prevent their carrying current with the positive voltage applied to the plates by the battery. A 60-cycle excitation voltage is applied to the grid through the grid transformer. This ex-

citation voltage has a greater magnitude than the negative d-c bias. When the excitation voltage drives the tube grid toward zero until it reaches a value less than the critical grid voltage, the tube fires, passing current through one half of the plate transformer. When the other tube grid is fired in the same way it passes current in the opposite direction through the other half of the plate transformer. The charge on the capacitor which has accumulated while one tube is firing is connected across the opposite tube by virtue of the firing, which drives the plate negative to interrupt the plate current. The a-c excitation voltage is supplied by a mechanical vibrator of the type that is used in automobile radios or by a vacuum-tube oscillator.

A particularly useful type of power supply has been built which functions normally as a regulated rectifier, taking power from the a-c commercial supply and floating the telephone-office battery. The instant that the power fails, relays convert the circuit from rectifier to inverter operation, and energy is taken out of the storage battery and supplied to the telephone circuits in the form of 60-cycle alternating current.

The control circuit for the combined rectifier and inverter is shown in Figure 9. A master control circuit is provided to measure the a-c supply voltage continually so that when its magnitude reduces to less than 85 per cent of its rated value the *GR* and *TR* relays are operated. The *TR* relay disconnects the rectifier and load from the a-c supply circuit and also reverses the polarity of the d-c supply to the plate transformer. The *GR* relay starts the 60-cycle exciter and switches the grid circuit from the regulated rectifier circuit to the fixed negative bias. The circuit then draws power from the d-c supply and generates a 60-cycle voltage which supplies the load and also supplies its own filament voltage through transformer T_2. When the commercial a-c voltage is restored, the master control circuit releases the *GR* and *TR* relays to restore the equipment to the rectifier operation.

The control of the rectifier output current is by the magnitude method, which has been described. The regulating circuit consists of the potentiometer *R*, grid battery, capacitor *D* and "varistor" *H*.

Table I. Regulated Rectifiers Used in Telephone Offices

Rectifier Output		Type of Regulation	Rectifying Means
Volts	Amperes		
48	0.001	Shunt thermistor	Disk type
20	0.006	Shunt thermistor	Disk type
85	0.23	Series vacuum tube	Vacuum tube
120–130	0.10	Series vacuum tube	Vacuum tube
95–180	0.10	Series vacuum tube	Vacuum tube
150–180	0.15	Series vacuum tube	Vacuum tube
17–45	0.60	Magnitude	Thyratron
48–74	0.6	Magnitude	Thyratron
120–130	0.6	Magnitude	Thyratron
34–48	3	Magnitude	Thryatron
24–34	10	Magnitude	Thyratron
24	30	Booster	2-element tube
48	10	Magnitude	Thryatron
48	30	Booster	2-element tube
130	2	Phase shift	Thyratron
130	8	Phase shift	Thyratron
130	3	Magnitude	Thyratron
130	10	Phase shift	Thyratron

Application of load to the battery reduces its voltage, and this change in voltage is reflected to the grids of the thyratron tubes to reduce their negative bias, which increases the charging current of the rectifier to bring the voltage back to the regulated value. The varistor *H* and capacitor *D* provide a voltage which varies directly in proportion to the a-c line voltage. This change is introduced into the grid circuit to compensate for line voltage changes.

In order to use the same ratio in the plate transformer for both rectifier and inverter operation, it was necessary to have different methods of adjusting the output voltage of the rectifier and the inverter. The output voltage of the rectifier is adjusted by means of taps on the primary of the plate transformer T_1 for charging different numbers of cells in the storage battery. This tap is selected at the time of installation. The output voltage of the inverter is adjusted for various a-c loads by means of the commutating capacitor C_1. The above rectifier then can be adjusted for charging a 132- or 142-volt battery and to provide any a-c output voltage between 210 and 250 volts with current loads of 2 to 5 amperes.

The time required to convert this circuit from a rectifier to an inverter is limited primarily by the operating time of the relays. The master control circuit requires about one cycle to detect a low voltage, and the power relays require about two cycles to make the transfer. Therefore the minimum practical time of transfer is such that approximately three cycles is lost between the time the commercial power fails and alternating current is available from the inverter to carry the load. The transfer back to the commercial power supply will be less because the master control circuit has time to function while the load is carried by the inverter, and the release time of the relays is smaller than the operate time. The practical loss in load voltage is approximately two cycles.

Rectifier Sizes

Table I shows the voltage and current output, type of control of the rectifiers, and the rectifying means that have found widespread use in the Bell System.

The regulated rectifier finds its applications in telephone offices where constant voltage, independent of load and a-c line-voltage variations, is required to supply filament grid bias and plate voltage to telephone repeaters. Certain measuring circuits require a regulated rectifier to supply a stabilized voltage. Regulated rectifiers also find applications where constant voltage is of secondary importance but an automatic power plant is desired for maintaining storage batteries in a fully charged condition to be ready to supply the power for telephone offices if the a-c power fails. A further compensation of regulating the voltage is the increase in life obtained from storage batteries if they are not continually being charged and discharged but are fully floated.

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The Dielectric Strength and Life of Impregnated-Paper Insulation—III

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IMPREGNATED-paper insulation, as used in high-voltage power cables, is subject, during the ordinary processes of manufacture, transportation, installation, and operation, to a wide variety of conditions, nearly all of which militate against the conservation of the original inherent properties of the insulation. Among these conditions are bending and other mechanical stresses, expansion and contraction due to temperature changes, original variations in manufacturing processes of drying and evacuation, and so forth. As a consequence, the electrical characteristics of the insulation of high-voltage cables, as for example power factor, dielectric loss, dielectric strength, and stability, all fall noticeably below those of samples prepared in the laboratory and generally are not subject to all the disturbing conditions mentioned above.

The uncertainties arising in the presence of these disturbing conditions and the inability to determine their relative importance has meant that most of the improvement in the quality of cables in recent years has resulted from empirical development and experiments on the finished cable. Cable engineers have been slow to adopt the results of laboratory research on controlled specimens, except perhaps in the clear-cut results of studies of such fundamental matters as drying and evacuation as related to the impregnation process, the purification of oil and paper, and the like. They have feared the effect of a change in a known variable on those which are still unknown.

The experimental studies described in this series of papers fall definitely in the controlled laboratory class. Effort has been made to eliminate the influence of all those disturbing elements in cable insulation except a particular variable under study. For example, the specimens tested are assembled and impregnated, cable fashion on rigid smooth conductors; they suffer no mechanical deformation, they are tested at one temperature, and in particular, they contain no free gas. On the other hand, the type of specimen and mode of testing permit the study of many variables in paper, oil, and mode of assembly of the usual type of cable insulation.

It will be evident that great care and perhaps reserve should be exercised in applying the results of these studies to the design and construction of finished cables. From what has been said, for example, there is an important condition obtaining in the usual solid type of cable, namely, free gas and gaseous ionization, which has been carefully eliminated in the present tests. It is most important, therefore, in such cables to consider the possible influence of the variations shown in these experiments in a number of other variables, on the volume and extent of gaseous ionization. On the other hand, it appears that many of the results of the present studies should be immediately applicable to the so-called oil-filled type of cable in that both in finished cable and laboratory specimens the conditions are closely the same. Even in this case, however, it is necessary to bear in mind the difference between a relatively short-life test in the laboratory and the long-time performance of finished cable.

Cable engineers are nevertheless deeply interested in these research programs and the results therefrom. They have shown this interest by suggestion, discussion, and other cordial support and co-operation. The researches have been supported by grants from The Engineering Foundation with the sponsorship of the American Institute of Electrical Engineers and have enjoyed the hearty co-operation of an advisory committee of manufacturers' and utilities' engineers as appointed by the latter body.

Scope of the Work

The first paper of this series is devoted to a study of the influence of the density of the paper on dielectric strength and life, the outstanding result being a noticeable decrease of dielectric strength with increasing value of paper density. The second paper reports studies of the influence of the thickness of paper on dielectric strength. The earlier studies on density occasioned some surprise and were contrary to the expectations of a number of cable engineers. Consequently the present paper describes primarily further experiments on the influence of the density of the paper, particularly in the use of a different oil, and also studies on the influence of oil viscosity, all directed to a possible explanation of the unexpected results on paper density. In addition, results of a study of the influence of the width of oil channels between convolutions of paper tape on dielectric strength are described. In all these experiments a very sensitive and rapid method of circuit interruption has been used, resulting in the great majority of cases in partial failures without burning. Followed by careful examination, dissection, oil extraction, and magenta staining, this has resulted in a very intimate picture as to the probable causes of failure in each case.

The Test Specimens, the Paper, and the Oils

The Test Specimens

The specimens were wrapped on brass tubes 48 inches long and one inch in diameter. The central or measuring electrode was of lead foil 16.5 inches long. At each end it was overlapped by a lead foil guard electrode 9.5 inches long, separated therefrom by one layer of paper. Beyond the guards the specimen was equipped with narrow angle conical ends of impregnated paper designed for uniform stress distribution.

The Paper

The cellulose wood-pulp paper was supplied by a well-known manufacturer in four values of specific gravity 0.703, 0.896, 1.136, 1.144, all of approximately 0.005 inch in thickness, and here designated as papers *A*, *B*, *C*, and *D* in the order of increasing density. The paper tapes were in all cases one inch wide.

Certain observations were also made on a fifth grade of paper (*E*), of 0.004 inch in thickness remaining from foregoing studies and manufactured five years earlier.

The test specimens were made by wrapping the paper tapes on one-inch-diameter smooth brass tubes, normally with $33^1/_3$ per cent and $66^2/_3$ per cent

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overlay with a butt spacing of tapes of between $^1/_{64}$ inch and $^3/_{64}$ inch. The normal thickness of the insulation wall was 0.080 inch, that is, 16 layers of 0.005 inch paper. Studies of different channel widths up to $^1/_8$ inch were made with 0.005 inch *B* paper.

Nine identical specimens were constructed for each value of density, thickness of paper, for each value of channel width, or other variable studied. In a few cases the number of identical specimens tested was reduced to six.

For details as to the construction of the specimen, methods of test, experimental data, accuracy, and so forth, the earlier papers may be consulted.[1]

The Oils

Two high-grade insulating oils having commercial numbers 5,314 and 5,317 were each tested over the entire range of paper variables as described above. Number 5,314 is a thin oil as used in oil-filled cables. Its values of viscosity at 40 degrees, 60 degrees, and 100 degrees centigrade are 17, 8.2 and 3 centipoises respectively. Oil 5,317 is a heavier oil somewhat darker in color and prepared for use in solid-type cables. Its values of viscosity at 40 degrees, 60 degrees, and 100 degrees are 480, 120, 18.3 centipoises respectively.

The specimens were impregnated in groups of three, normally at 60 degrees centigrade. In certain studies of the influence of viscosity on impregnation, the impregnating temperature of 5,314 was reduced to 40 degrees and that of 5,317 raised to 100 degrees, these temperatures giving the same value of viscosity for the two oils.

Breakdown and Stability Tests

The accelerated step-up life tests started at 400 volts per mil. The voltage was increased by 3.12 per cent every four hours, thus giving a geometric increase to 700 volts per mil in three days. The continuous application of voltage was interrupted at intervals (usually every four hours) for tests of power factor, capacitance, and internal gaseous ionization.[2] In this program the average life of a specimen was from two to five days. It has been assumed that this brief duration eliminates the influence of chemical change. Long experience has shown that there is no free gas in these specimens. Consequently the results appear to isolate definitely the influence of each of the several variables studied, that is, paper density, paper thickness, oil viscosity, and so forth. The design of the specimen and close attention to uniformity of construction have ensured that the failures have occurred almost invariably under the central electrode of the specimen.

Results of Tests

Density of Paper

The results of the tests on four values of paper density using 5,314 oil have been published in an earlier paper.[3] For convenience some of them are reproduced in Figures 1 and 2. In the same figures will be found the results of later studies on the same papers as impregnated with 5,317 oil, and under the same conditions as to impregnation, test program, degree of accuracy, and so forth.

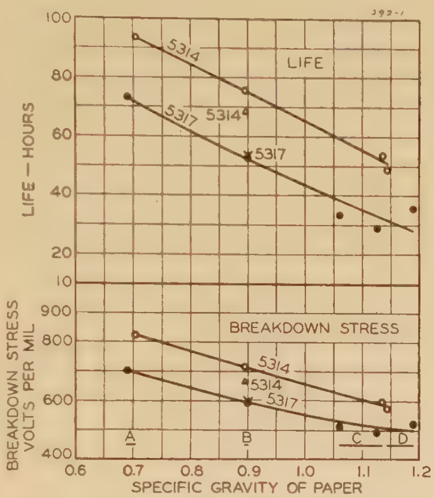

Figure 1. Stability of oil-impregnated-paper insulation—life and breakdown stress

Impregnation at a pressure of 2.0 millimeters of mercury

Δ—40 degrees centigrade

x—100 degrees centigrade

All others—60 degrees centigrade

Two noteworthy features are indicated:

1. The decrease of dielectric strength and life with increasing paper density, already reported for 5,314 oil, is also found to obtain with close parallelism for 5,317 oil.

2. Values of dielectric strength and of life using 5,317 are uniformly lower than those for 5,314.

The impregnating oil content is somewhat higher for 5,314, particularly at the low values of paper density. The power factor for 5,314 is also higher. The trend of these curves has been discussed in the earlier papers.

Paper Thickness

The studies of the influence of paper thickness with 5,314 oil only have been reported in an earlier paper.[4] Figure 3 is reproduced therefrom. It will be noted that there is a pronounced and uniform decrease of breakdown strength with increasing thickness of paper ($21^1/_2$ per cent decrease from 0.003 inch to 0.008 inch thickness).

Saturation Studies

For each group of tests the percentage of oil content by volume was determined for one or more specimens. Thus in Figure 2 the decrease of percentage of oil with increasing paper density is readily noted. It is also noticed that specimens impregnated with 5,314 oil contain a higher percentage of oil, other things being equal, and that this is accompanied by higher dielectric strength. The same parallelism has been observed in other tests. In order to determine whether viscosity of the oil had any bearing on these differences, six specimens of *B* paper were impregnated with 5,314 oil at 40 degrees, and six others with 5,317 oil at 100 degrees, the viscosities of the two oils having the same value at these temperatures. All twelve specimens were subjected to life test at 40 degrees centigrade, in accordance with the standard program.

Results of these comparative studies are indicated as single points giving the average values in Figures 1 and 2. The

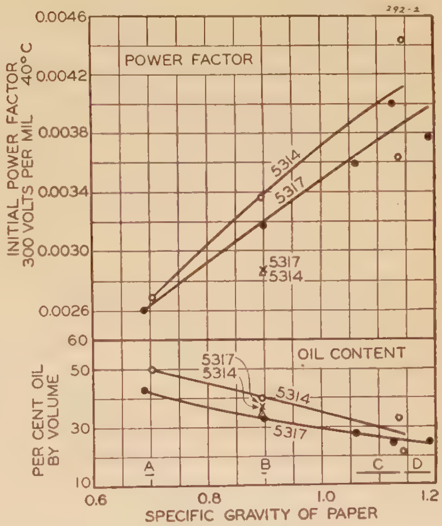

Figure 2. Stability of oil-impregnated-paper insulation—power factor and oil content

Impregnation at a pressure of 2.0 millimeters of mercury

Δ—40 degrees centigrade

x—100 degrees centigrade

All others—60 degrees centigrade

order of accuracy is about the same as that in the paper-density studies (plus or minus deviation five to ten per cent). It will be noticed that the change of viscosity of 5,317 oil results in no appreciable change in either dielectric strength or life, notwithstanding a rather wide variation

in the value of viscosity (480–120 centipoises). On the other hand, impregnation with 5,314 oil with a considerably smaller variation of viscosity results in a slight lowering of both dielectric strength and life, although the values are still substantially higher than those for 5,317.

As to oil content, in the case of number 5,317, large decrease in viscosity between 60 degrees and 100 degrees results in the small increase of oil content of from 33 per cent to 37 per cent. In the case of 5,314 oil the increase of viscosity between 60 degrees and 40 degrees in these comparative studies results in a reduction from 40 per cent to 34 per cent. When the viscosities are the same, the oil content and power factor are about the same.

Width of Channel

The influence of the width of channel on breakdown strength and accelerated life was studied between the limits of a close butt joint and a maximum channel width of $^1/_8$ inch, observations being taken at one intermediate thickness, $^3/_{64}$ inch. Observations were also made on six specimens having a $^1/_{64}$-inch overlap, for one of the oils. Otherwise nine specimens were tested at each channel width and for each of the two oils, 5,314 and 5,317.

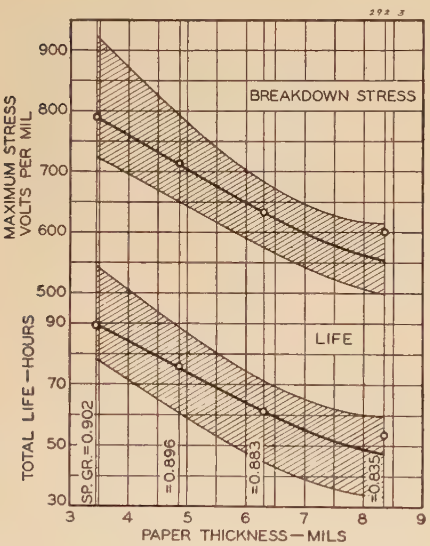

Figure 3. Stability of oil-impregnated-paper insulation—breakdown stress and life

The results of these studies are plotted in Figure 4, in which the average spread of the nine specimens represented by each point is indicated in vertical lines.

It will be noted that the decrease of dielectric strength with increasing width of oil channel is practically linear for oil 5,314, the percentage decrease from a close butt joint to a channel width of $^1/_8$ inch being 9.5 per cent. A similar over-all decrease is seen for oil 5,317, although the rate of decrease with channel width is less regular. The over-all decrease in this case is roughly six per cent. No great accuracy may be claimed for numerical values in these results, principally because it is difficult to maintain a uniform channel width of low value. Thus in the close butt experiments, the results on two specimens have been omitted, because on dissection it was found that the joints between tapes were slightly open in the region of failure. The closest butt joint that can be wound seems to have here and there a slight opening. With this in mind a set of specimens was wound with a $^1/_{64}$-inch overlap,

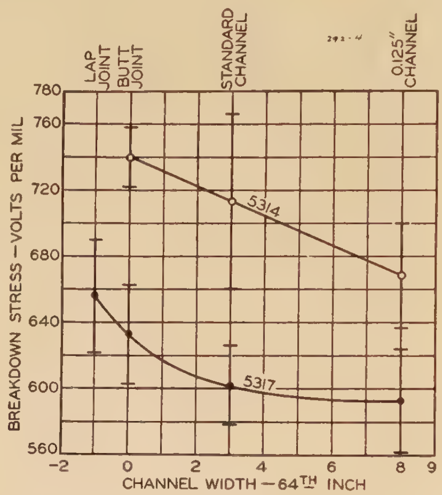

Figure 4. Stability of oil-impregnated-paper insulation—breakdown stress versus channel width

and it may be seen from the curve for oil 5,317 that this resulted in a still further slight increase of dielectric strength.

It is to be noted that the higher dielectric strength of oil 5,314 over 5,317 noted in the other experiments, appears also in the experiments on channel width.

Types of Failure

Initial and Partial Failures

An early method of interrupting the circuit following breakdown of the specimens, using a very light fuse in the primary circuit of the testing transformer, resulted uniformly in through failures with considerable burning. Later, use was made of a thyratron tube, operated from the voltage over a resistance in series with the connection of the low-voltage electrode to ground, and operating a fast-moving relay and circuit breaker in the primary circuit. This resulted in early interruption of failure and indeed enabled us to remove and examine a great many specimens in which partial failures only had occurred. These partial failures consisted of the burning, or break through, or other evidence of trouble, in from one layer of paper or channel of oil, to an increased number of layers up to but short of the full insulation wall. The amount of burning in a failed specimen of this type is very greatly reduced, and a much better idea of the origin, causes, and succeeding progress of the failure has been obtained.

The failures may be roughly divided into four groups based on careful dissection followed in many cases by oil extraction and staining with magenta:

A. Through radial failiures.

B. Partial failures involving one half of the total thickness of the insulating wall or less, and all terminating on the high-voltage conductor, or on the outer lead sheath.

C. Partial failures within the insulating wall, but not reaching either conductor or sheath.

D. Failures with no outward evidence of burning, but with subsequent evidence of wax formation.

Out of a total of 117 specimens tested, 46 had complete through failures and 66 had partial failures of one type or another.

Class A—Through Radial Failures. In these cases the burning is greater than in the partial failures, but in general the evidence is that there is little departure from an approximately straight radial failure ending in an oil channel near the outer layers and the first adjacent layers. Tree patterns and tracking are often evident, but only to the extent of reaching one adjacent oil channel. Puncturing through the tape of four consecutive layers was not uncommon.

Class B—Partial Failures Ending on Inner or on Outer Conductor. The characteristic of most of these failures is an area of blackened or burned oil film, never greater than one inch in diameter, towards the center of the insulating wall. Beyond the burned spot, evidence of trouble diminishes rapidly within one or two layers, then disappears. In the other direction burning also decreases, the puncture taking the form of short lengths of blackened oil in the channels and puncture through tapes, becoming smaller and cleaner towards the electrodes. The first, second, or third oil channel from the high-voltage conductor is usually involved. An exceptional case in this group consisted of four tiny punctures of four outside layers, originating in the oil adjacent to a corner of reinforcing paper at the end of the specimen and with no evidence of burning.

Of a total of 66 partial failures, 11 terminated on the outer electrode and 49 on the inner electrode. In one case there was a partial failure to each electrode, the two being well separated longitudinally, and apparently not related.

Class C—Partial Failures Not Reaching Either Electrode. Only three failures of this type were noticed, but they were quite definite. They were characterized by iso-

lated black regions in an oil channel. Two were within four layers of an electrode. In one case the trouble was confined to only two layers.

In addition to these clearly marked local failures, there were ten cases in which this type of trouble was found to be coexistent with a failure either "through" or "partial" ending on an electrode, although definitely separated from it.

Class D—Failures With No Outward Evidence of Burning. In two cases first examination of the specimen failed to reveal any trouble other than a slight smell of burned oil. After subsequent oil extraction and staining with magenta dye, there was evidence in each case of initial wax formation at various places.

In considering these results, it should be borne in mind that in our specimens the insulation wall was relatively thin (0.08 inch), and the diameter of the smooth high-voltage conductor was relatively large (one inch). The excess of stress at the conductor surface over that at the outer electrode is of the order of 16 per cent, which is not much greater than the values of the average spread of our experimental breakdown results; thus partial failures originating on the outer electrode may be due to normal variations in the paper or oil. The smooth high-voltage conductor is perhaps an advantage in that it eliminates the common type of oil pocket in stranded conductors and confines the experimental results more definitely to the impregnated wall.

Throughout the careful examinations of the failed specimens, constant evidence has been found that the initial trouble arises in an oil space. In the cases of partial failures, either the first or the second oil channel was always involved. In all cases involving one, two, or three layers only, punctures were rarely found, the trouble being of the nature of oil blackening and surface tracking. Also in all cases without exception it was found that the oil in the channel next to the high-voltage conductor was more or less blackened, in many cases the blackening being limited to the edges of the paper and revealed by fine black lines on the polished conductor.

Discussion

It is believed that the outstanding results of this and the foregoing papers may be explained qualitatively in terms of the characteristics of the impregnating oil.

All refined degassed insulating oils apparently have the following characteristics in greater or less degree:

(*a*). A relatively low long-time conductivity, that is, as measured after a long application of continuous electric stress. This conductivity is partly electronic, but largely due to an inherent electrolytic dissociation arising in the complex molecular structure.

(*b*). An initial or short-time conductivity persisting for, say from one-tenth to one second, which may be many times as great as the short-time conductivity. This conductivity is due to an accumulation of relatively large and heavy ions or clusters of neutral molecules about a central ion. It is the motion of these large ions back and forth under alternating stress which causes the greater part of the dielectric loss in such oils.

(*c*). Both types of conductivity are increased by contact with paper and metals, due to added electrolytic impurities, and also to increased electrolytic dissociation.

(*d*). Application of continuous stress causes the large heavy ions to move to the electrodes or limits of an oil space, where, owing to their envelopes of neutral molecules, not all of them are discharged, and space charges accumulate close to the electrodes or other limiting surfaces, for example, paper tapes. Under alternating stress a similar action goes on resulting in maximum space charge accumulations at the crest of each half cycle. Studies of the mobility of these ions indicate that at 60 cycles there is sufficient time during a half cycle for some at least of these ions to pass completely across the oil channels occurring between paper tapes in impregnated-paper cable insulation.

The mass of evidence from the failures studied in this series of papers is that failure begins in an oil channel between paper tapes and probably at the contact surface between paper and oil. The failure is caused by a layer of high stress between the paper and a space charge layer immediately adjacent. Witness for example the spiral marking on the high-voltage conductor immediately under the edges of the paper tapes and over the full length of the specimen; also the isolated cases of partial failure in which the only evidence of failure is blackening in the channels, with carbonized channels through paper tapes spreading therefrom.

The ultimate cause of failure is a stress sufficiently high to ionize a molecule with the liberation of gas. The tiny bubble of gas may be reabsorbed before the next voltage crest in which case increasing carbonization may result before final failure. With further increase of stress, however, the gas bubble becomes larger, continues through the cycle, and is the seat of gaseous ionization. This latter attacks the adjacent paper much more aggressively, leading ultimately to puncture, to the extension of the oil breakdown in the next oil film, and to surface spreading to the adjacent oil channel of the next layer.

Failures here described, in outward appearance are often similar to those described by Robinson.[5] However, there is much indication in this work that the "coring" in the paper tapes occurs at the end rather than at the beginning of the process of failure. Coring has been noticed frequently at both conductor and sheath, but in these cases the complete absence of burning, as compared with conditions in the oil channels of deeper lying layers indicates strongly that the initial trouble originates in the channels then extending to puncture of the paper by the coring process. In our case we picture the oil as breaking down with the liberation of gas, subsequent gaseous ionization attacking the paper wall, perhaps in the manner suggested by Robinson. However, Robinson postulates a free gas bubble originally present near the high-voltage conductor, gaseous ionization in this bubble, and coring as the first stage of failure in the insulation wall itself. We believe our specimens to be originally entirely free of such gas bubbles and that the first gas that appears is due to oil failures. We do not find that coring is limited to the inside layers, due to the small difference in radial stress between outer and inner layers in our specimen. However, in the cases discussed by Robinson the higher stresses on the inside layers would tend to give first oil failures and subsequent coring in those layers.

The decrease of dielectric strength with increasing density of the paper tapes has already been ascribed to the increasing value of the stress in the oil channels due to the increasing value of dielectric constant of the impregnated paper. Approximate computation of the breakdown stress in the oil indicates a slight decrease in the critical stress with increasing paper density.[3] It is suggested that this is explained by the fact that the oil in contact with papers of increasing density takes on a greater number of adsorbed ions from the paper. There is a resulting increase in the density of the space charge layers, and an increase in stress between this layer and the adjacent surface. Thus the critical or breakdown stress in the oil is reached at a lower voltage or lower average stress.

Some cable engineers have been loth to accept this evidence that the dielectric strength of impregnated paper decreases with increasing paper density. It is stated that in manufacture, cables containing high-density paper give a higher dielectric strength than those using paper of a lower density. In one case it is stated that it has certain other advantageous physical properties. There are, however, no published data clearly showing these advantages of high-density paper in comparative life tests under sustained stress.

One possible explanation is that there

may be a difference in behavior under impulse or short-time tests and under sustained voltage tests. Here too experimental data is very meager. Del Mar and Works have recently presented data indicating that under short-time tests for certain values of overlap of paper tape, the breakdown strength of high-density paper may rise above that of low-density paper. However, as emphasized elsewhere in this paper, a time element undoubtedly is present in failures under sustained high stress. It is possible, therefore, that under impulse or short-time tests the inherent high dielectric strength of the high-density paper might play a more important part than the delayed breakdown of oil in the oil channels, in which the stress is much higher than the over-all average stress, due to the difference in the dielectric constants of the impregnated paper and the oil in the channels.

A pronounced decrease in dielectric strength with increasing paper thickness has been found.[4] This result may be attributed to the well-known fact that the breakdown strength of thin oil films decreases with increasing thickness. However, in this case also, approximate computations of the values of stress in the oil films at breakdown decrease with increasing thickness, again giving a first indication that the breakdown stress of the oil does not have a constant value. The same explanation may be offered here, namely, that with increasing paper and channel thickness, more ions are present in the oil film, greater values of space charge accumulate, and a critical breakdown strength of the oil is reached at a lower value of over-all stress.

Conclusions

1. Laboratory studies in accelerated life tests of the influence of the density of the paper on the breakdown strength of impregnated-paper insulation have been continued. When impregnated with a heavier oil, as used in solid-type cables, there is a marked decrease of dielectric strength with increasing paper density. Results are closely similar to those already found using a thin oil as impregnant.

2. Raising the viscosity of the thin oil and lowering the viscosity of the heavy oil so that both have the same value during impregnation has little effect on the breakdown strength. Specimens impregnated with the heavy oil show no change in behavior. Raising the viscosity of the thin oil results in a slightly lower dielectric strength. When the two oils have the same viscosity, the values of power factor and of oil content are approximately the same.

3. The dielectric strength of impregnated paper decreases with increasing oil-channel width between successive convolutions of tape. When the thin oil is used the decrease is about 9 per cent; when the heavy oil is used, the decrease is about $6^1/_2$ per cent, as the channel width increases from zero (close butt joint) to $^1/_8$ inch.

4. In all the foregoing experiments the dielectric strength and stability when using the thin oil as impregnant were noticeably higher than when using the heavy oil.

5. Close examination of all failures leads to the conclusion that in the accelerated life or stability tests lasting two days or more, failure always begins in an oil space.

6. Qualitative explanations are offered for results in this and two preceding papers of this series.

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Frequency Control of Load Swings

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Synopsis: The Consolidated Edison Company made a series of tests to determine the cause and extent of system load swings on generators and tie feeders. The results were reported in detail in a previous paper.[1] An analysis of these tests indicated ways in which our load dispatching methods could be improved. This paper reports the effect of the changes in the method. Our experience indicates that the operators may reduce tie and generator load swings to negligible values by using frequency to control manual adjustments of generator output, provided that an accurate frequency indication is available at each generating station.

SEVERAL of the principles reported in the previous paper[1] have been applied to our method of load dispatching to reduce the load swings on the tie feeders and generators. These principles are:

1. Major load swings are caused by manual operation of governor valves in one or more stations during periods of load change.

2. Governors or supplementary controllers do not contribute to load swings but operate continuously to check them.

3. A change in voltage will result in a change in both real and reactive load demand, but a change in frequency within reasonable limits and with constant voltage does not result in an appreciable change in system demand.

4. The instantaneous relations between load, voltage, and frequency serve to indicate the initiating cause of a load change. An initial change in any one of these quantities will affect the other as shown in Table I.

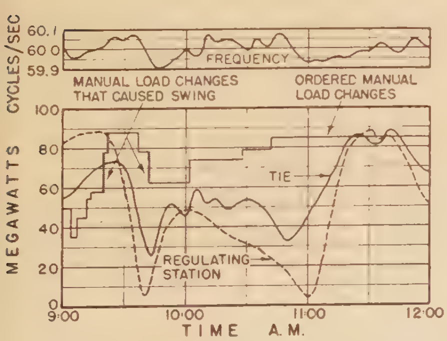

Figure 1. Load swing caused by load changes ordered by the system operator

The load changes were made when they drove the frequency away from normal

Table I

Initiating Change	Effect on Voltage	Effect on Load	Effect on Frequency
Voltage			
Increase	—	Increase	Decrease
Decrease	—	Decrease	Increase
Load			
Increase	Decrease	—	Decrease
Decrease	Increase	—	Increase
Frequency			
Increase	Increase	Increase	—
Decrease	Decrease	Decrease	—

Effect of Manual Adjustments of Generator Output

On a system supplied from one turbogenerator, a manual adjustment of the steam input would be readily discernible in the speed and voltage. This effect is not so apparent on a large system, because there are many governors and controllers operating to compensate the change. A change in steam input in excess of the load requirement on the one unit system results in a relatively large speed change, while on the large system, it results in a load swing over the connections between generators. On a large system the generator on which a manual load adjustment is made will have a simultaneous change of speed and load in the same direction, while all other units, under the influence of their respective governors, will change load in a direction opposite to the frequency.

If the generator ouput at one station is increased by manual adjustment when the frequency is at or above normal, the governors at the other stations will reduce the generated load by an equal amount. If these other stations are operating on scheduled loads the station operators will make manual adjustments to restore their stations to schedule, thus driving frequency still higher. This jockeying of load between stations results in load swings on the tie feeders, as well as on the stations, and imposes an unnecessary burden on a station assigned to regulation of frequency.

An example of load swing caused by manual adjustments without regard to instantaneous frequency is shown in Figure 1. This type of load swing can be avoided if the system frequency is used as a control in making manual adjustments.

Load-Dispatching Methods

It was the practice of the system operator to assign blocks of load to each station from time to time on the basis of an advanced estimate of the load requirements. Past experience with the hour-to-hour changes in total system demand was used in making the estimate. The station operators made the load changes when ordered, regardless of the frequency at that time. If the system demand did not change exactly as anticipated, the ordered changes would cause the frequency to depart from normal, and a

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system-wide load swing would result as the governors and controllers operated to compensate for the excess change. Where the tie feeders are relatively small in capacity, or where for contractual reasons the tie feeder interchange is to be maintained at a fixed value, it is then necessary to make additional adjustments to restore the tie-feeder load to normal.

In order to minimize these load swings, the following changes were made in our load-dispatching methods. The system operator now estimates the load in advance but assigns load to each station in larger blocks and at less frequent intervals. When the station operator receives a load-change order, he executes the order only at an instant when the change will tend to restore the frequency to normal. In other words, the station operator increases load only with low or dropping frequency and decreases load only with high or rising frequency. Thus, errors in load anticipation which could cause load swings are automatically eliminated.

The system operator has authority to make load changes, regardless of frequency, should changes be necessary, but he must specifically state that this shall be done.

We would like to point out that these measures are not intended to maintain closer frequency or to keep the accumulated time error within narrow limits, but are for the purpose of reducing, by the use of indicated frequency, the unnecessary load swings on generating facilities and tie feeders.

Figure 2 shows the extent that load swings have been reduced by the present method of load dispatching.

Need for Accurate Frequency Indication

Frequency indication as a control in making manual load adjustments requires that each station have a suitable frequency indicator. Since the normal frequency band on the system is about 0.1 cycle per second, an instrument for this purpose would have to be accurate within a fractional part of this normal band. This accuracy should be in the neighborhood of one part in 50,000, which is beyond the capability of the conventional frequency meter. A meter of normal accuracy could be used if one meter sent impulses to all stations indicating whether the frequency was high or low. Another way would be by transmitting a standard frequency voltage and comparing it differentially with another voltage at system frequency at each station.

On our system a standard frequency accurate to better than one part in a million is available.[2] This standard frequency is transmitted to each major station and compared with the system frequency by means of a synchroscope. The synchroscope gives one revolution for each cycle of slip frequency between the standard and system frequencies. The direction of rotation indicates whether the system frequency is high or low, and the rate of rotation is a measure of the amount of departures from standard frequency. Each station operator therefore has the same indication of system frequency.

This method of transmitting a frequency standard has been practical on the Consolidated Edison Company system because of the relatively few large stations and the short distances between them. In order to use a similar scheme over a widespread system, we have developed a frequency indicator operating from a frequency standard received by radio. Figure 3 shows the schematic diagram of this device. This scheme is based upon sending a standard frequency by a radio transmitter which has sufficient signal strength to cover the area of the system.

The radio signal is amplified by a radio receiver at each station, and the amplified standard-frequency voltage output is impressed on a step-up transformer. The transformer supplies a neon tube which is circular in shape and is covered by a disc having a single slot. The disc is driven by a synchronous motor from the power system. The standard frequency in cycles per second must be exactly divisible by the motor synchronous speed in revolutions per second. When the device is in operation, and the neon tube is viewed through the slotted disc, a series of light spots evenly spaced in a circle will be seen. If these spots remain stationary, the system frequency at that time is correct. If the spots rotate, the system frequency is incorrect. The direction of rotation indicates whether the system frequency is above or below the standard frequency.

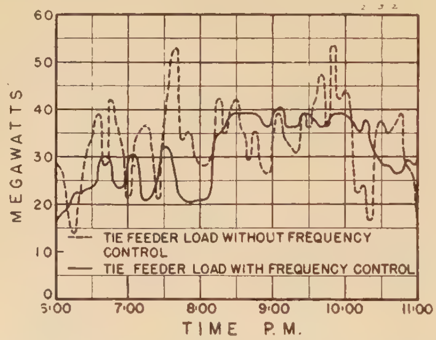

Figure 2. Effect of frequency control of manual load changes on tie-feeder loads

When load changes are made independent of system frequency, the tie-line loads vary considerably. When the load changes are made so that they restore frequency to normal, the large rapid load changes are not experienced

A few trial installations of this device are in service using an existing standard-frequency broadcast by the Bureau of Standards in Washington. This signal is broadcast on a carrier frequency of five megacycles and modulated by a 440-cycle tone. A 1,200-rpm synchronous motor is used to drive the slotted disc. The experience with this device has been very satisfactory.

Conclusion

The results of a year's experience using frequency as a control in making manual load changes on the Consolidated Edison Company system have led us to the following conclusions:

1. Generating-station and tie-feeder load swings can be reduced to a point where load swings are no longer a problem.

2. The need for accurate load anticipation becomes less important.

3. Tie feeders can be loaded safely to their thermal or stability limits.

4. Interchange energy between parts of the system may be controlled within closer limits. For contractual reasons this is often important.

5. Boiler-room maintenance costs can be decreased by reducing the load swings on the boilers.

6. Manual load adjustments by the station operator can be normally made in a direction to aid the automatic governors or controllers.

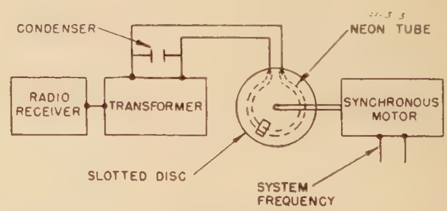

Figure 3. Schematic diagram of stroboscopic method of frequency indication based upon a standard frequency received by radio

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The Effect of Initial Conditions on Subharmonic Currents in a Nonlinear Series Circuit

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Synopsis: This paper shows the importance of switching time as an initial condition for the current in a simple nonlinear series circuit with a saturable inductor. It is also shown that initial flux linkages in the inductor have an important effect on the current response during the first few cycles after closing the switch. It is emphasized that all experimental work was done with voltages below the critical ferroresonant voltage.

IT is the purpose of this paper to report the results of a study of the effect of switching time on the current response in a series *R*, *L*, *C* circuit containing a saturable inductor. In previous experimental work no attempt has been made to control the initial angle of the sinusoidal applied voltage.

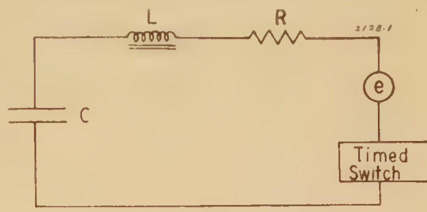
This series nonlinear circuit has been the subject of considerable investigation. Its importance was recognized in this country when outages caused by abnormally high currents were experienced in transmission lines equipped with series capacitors. It was found that the observed abnormal currents had a frequency lower than the system frequency. The frequency was a submultiple of the fundamental; hence, they were called "subharmonic" currents. Concordia[1] and Butler solved the equations of the circuit for the parameters of interest in their transmission-line problem by employing a differential analyzer. Following this work Travis[2] and Weygandt approached the problem analytically with the method of matching boundary conditions. They were forced to make many simplifying assumptions, but the solutions they obtained give an insight into the mechanism of the production of subharmonic currents in this circuit. One of the most serious assumptions, namely, that of neglecting capacitance in the circuit when the inductor is saturated, was removed by I. Travis[3] in a later paper. The theoretical investigation has reached a point where a great amount of computation must be done to obtain a small amount of information. To study the performance of this circuit further J. D. McCrumm[4] has employed an experimental method which consists of a systematic variation of circuit parameters, and recording of wave forms on oscillograms. The currents in his circuit were initiated by the random closing of a knife switch.

Experimental Procedure

The circuit studied is shown in Figure 1. The timed switch is constructed so that it can be closed at any point of the 60-cycle sine wave. After closure, it remains closed for the duration of four cycles, and then opens. Two cycles later it closes again at the same switching angle as before. This repetitive closing and opening enables the experimenter to view the first four cycles of the current as a steady image on an oscilloscope screen. During the two cycles that no voltage is applied, the capacitors are short-circuited to insure that the initial charge is zero. With this timed switch it is possible to watch the change of wave form as the switching angle is varied and also while various circuit parameters are varied. Provision was made to keep the switch closed after one of its periodic closures. This enables the experimenter to make oscillograms of the current from the time of initiation. Details of the timed switch are given in the appendix.

Results

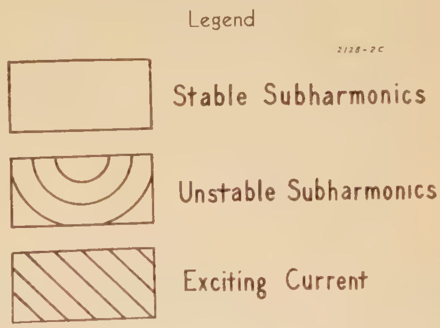
The effect of initial voltage phase angle on the circuit response is best shown by

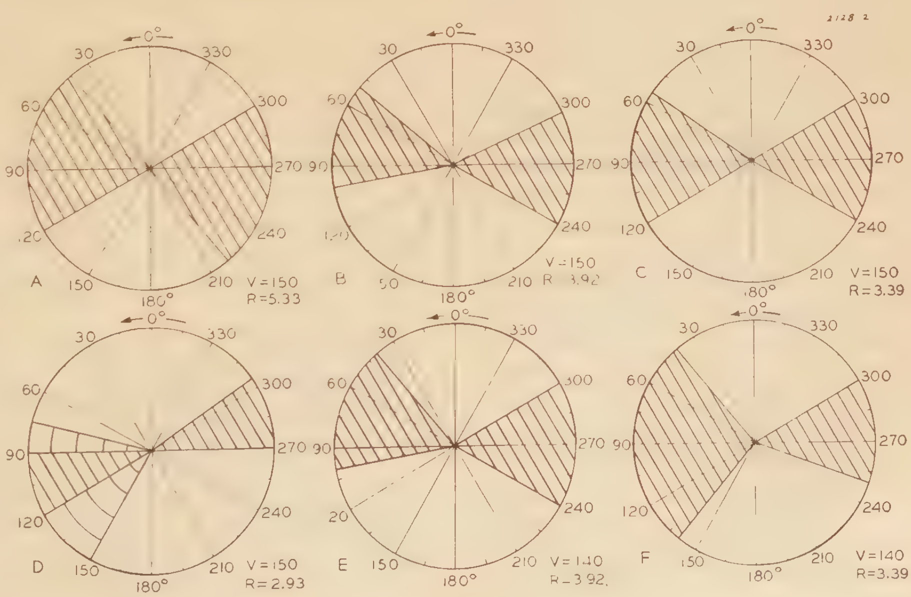


L = G.E. Transformer 9TM754A1
C = 355 Mfds. (Leakage 2000 Ohms)
R = Varied
e = 12 E sin (ωt + φ)

Figure 1. Circuit diagram

Figure 2. Polar diagrams showing the type of circuit response for the full range of initial voltage phase angle





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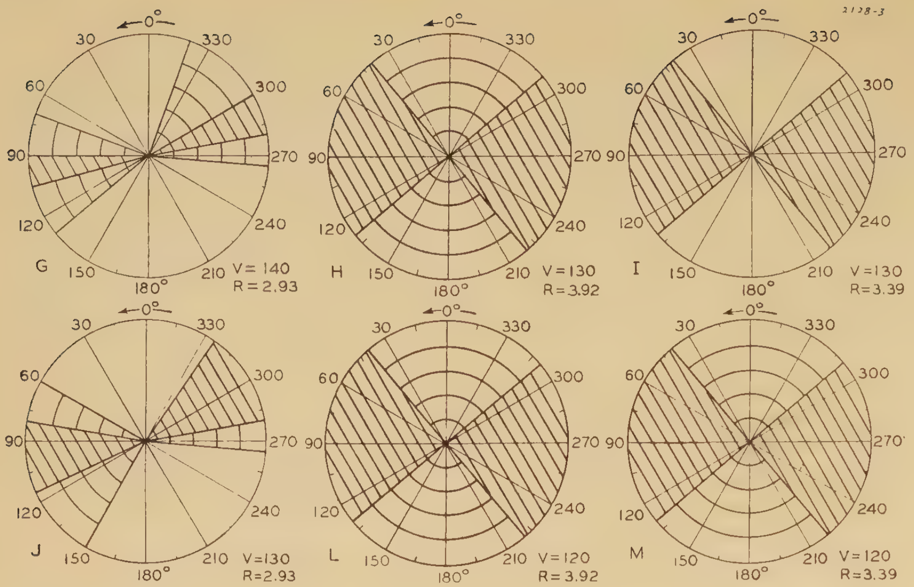
The author wishes to express appreciation to his adviser, C. N. Weygandt, for help, and to the General Electric Company for plans of the relay employed in the experiment. This work was done at the Moore School of electrical engineering, University of Pennsylvania, Philadelphia, Pa., during the term 1939–40. Financial assistance was given by a Moore School fellowship.



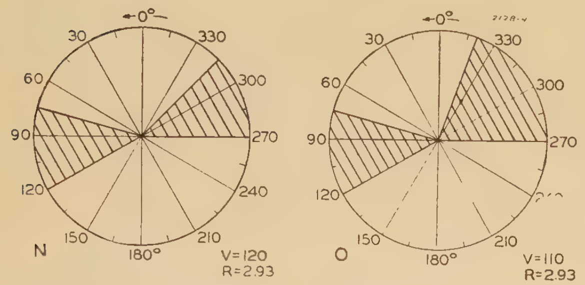


Figure 3 (above)

Figure 4 (left)

the polar diagrams of Figures 2, 3, and 4. The zero of angle was arbitrarily chosen as the zero on an oscillogram before the negative half-cycle.

The response of the circuit may be divided into three types, namely:

1. Exciting current.
2. Stable subharmonics.
3. Unstable subharmonics.

Exciting current is the normal current in a transformer primary for no load on the secondary. Stable subharmonics are abnormally high currents having a frequency which is an integral submultiple of the frequency of the applied voltage. The currents are said to be stable if they persist as long as voltage is applied to the circuit and they maintain a repetitive wave form. Unstable subharmonics are abnormally high currents having a nonrepetitive wave form. According to the theory of Travis and Weygandt, these currents have imperfectly matched boundary conditions, and therefore they cannot repeat each cycle exactly. It is common for such currents to continue in a "jittery" state for many minutes and suddenly disappear not to reappear until the circuit is again closed with the proper initial conditions.

The wave forms of the subharmonics are not given here, since many have already been published.[4] The initial voltage phase angle has no effect on the wave form of the subharmonic current in the region where these currents are possible. Switching angles of 90 degrees and 270 degrees generally give an exciting-current response, and the unstable subharmonics are situated on the border line between exciting current and stable subharmonics.

The diagrams of Figures 2, 3, and 4 show also that the subharmonic region is smaller for high values of circuit resistance. In the circuit of Figure 1, subharmonics could not be initiated at any angle when the resistance R was greater than approximately seven ohms. The extent of the subharmonic region also decreases as the applied voltage decreases. This is to be expected, because the lower the voltage, the more difficult it is to saturate the core of the inductor.

The boundary lines between current regions are drawn sharply but actually it is difficult to tell within plus or minus five degrees where one region ends and another begins. These graphs then have only a semiquantitative value. Each diagram is the result of observations taken at least two complete cycles of 360 degrees to be certain that the results are reproducible.

Another possible initial condition which has not been previously investigated either theoretically or experimentally is the initial flux in the inductor core. Oscillograms were taken with the transformer winding flashed with d-c before the switch was closed. Figure 5 shows two oscillograms taken of current initiated with identical initial conditions, except that the inductor was flashed in one direction for one oscillogram, and in the reverse direction for the other. It can be seen that the first few cycles are very different, but the wave forms after a short time has elapsed are identical. Figure 6 shows a repetition of the experiment for a different set of initial conditions.

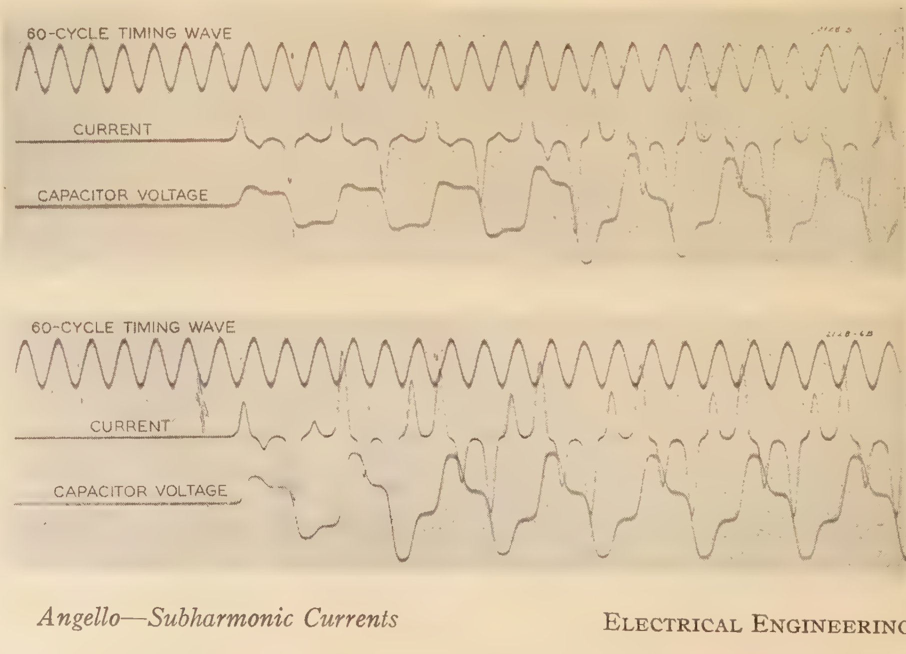
Conclusions

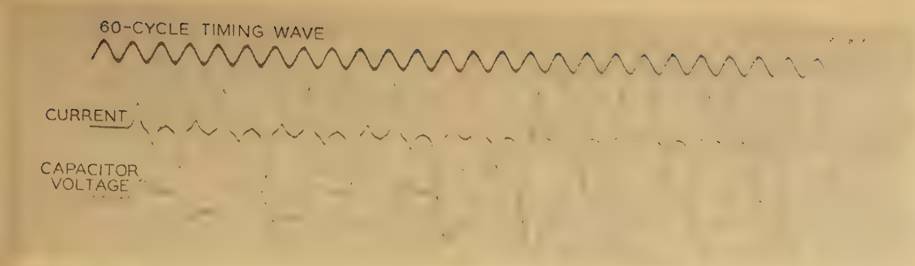
The results of this study may be summarized as follows:

(*a*). The current response in this circuit is dependent on the initial voltage phase angle.

Figure 5. Oscillograms showing the current response of the circuit for identical initial conditions except that the initial flux in the inductor core was reversed

The initial voltage angle was 31 degrees. Other data are R=2.93 ohms, V=120 volts, and q_0=zero





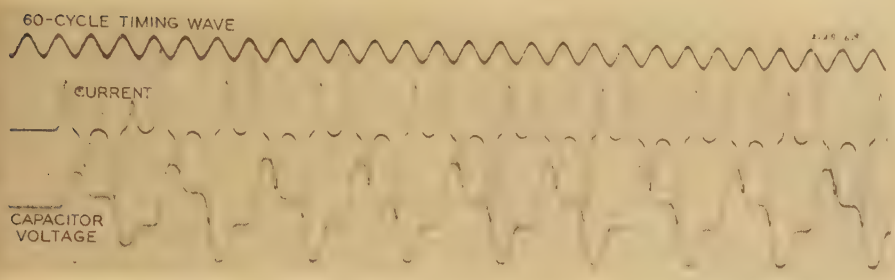


Figure 6. Oscillograms made in a manner similar to those of Figure 5

In this case the initial phase angle was 266 degrees. Other data are $R=2.93$ ohms, $V=140$ volts, and $q_0=$ zero

(*b*). The wave form of stable subharmonics is independent of initial phase angle.

(*c*). The wave form of the first few cycles of a subharmonic current is dependent on the value of initial flux linkages in the inductor.

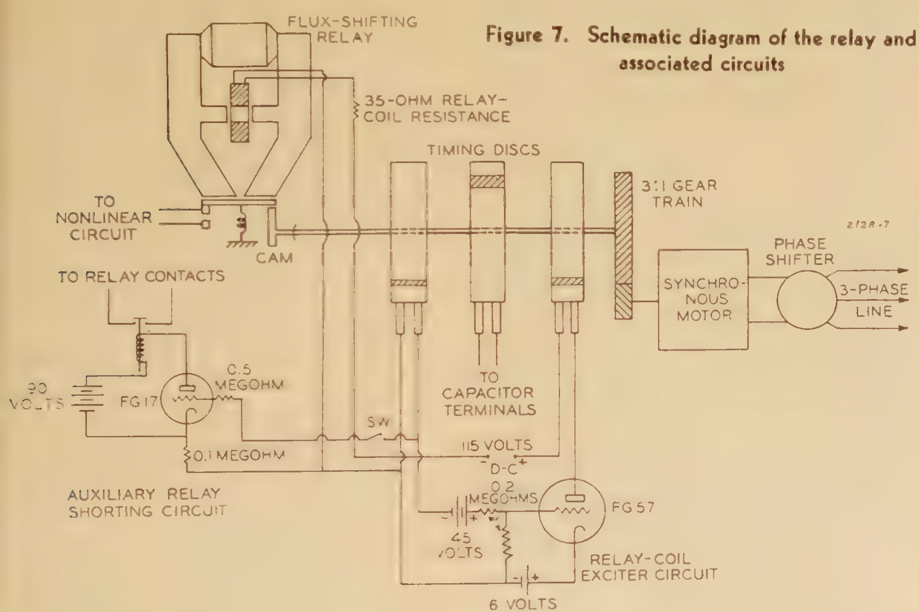
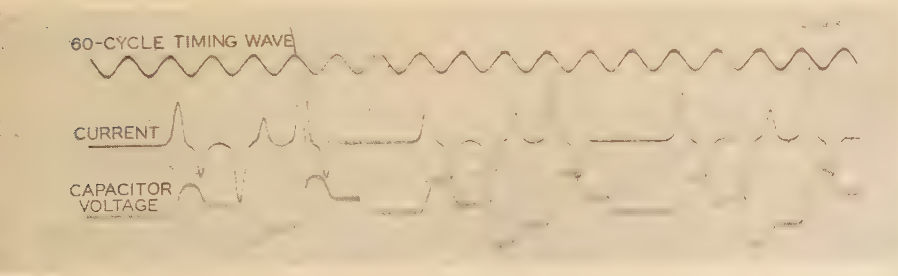


Figure 7. Schematic diagram of the relay and associated circuits

Figure 8. An oscillogram illustrating the performance of the timed switch

Note that the wave forms are repeated in detail. Note also that the capacitor voltage is reduced to zero after the switch opens so that $q_0=$ zero for the next closure



It should be noted that nothing has yet been said about ferroresonance. The impressed voltages employed in this work were lower than the critical voltage for ferroresonance in all cases. All statements concerning initial conditions, therefore, apply only to subharmonic currents. It is certain that the currents of ferroresonance are abnormally high, but the frequency is that of the applied voltage. This point is stressed, because some confusion is apparent in the discussions of other papers.

Appendix

The timed switch discussed under experimental procedure was a mechanical flux-shifting relay. A mechanical relay was employed, because it is necessary to have the circuit resistance very low. This eliminates the possibility of using electronic switches, because they have a high internal resistance, and furthermore this resistance is a function of the current.

Figure 7 shows a schematic diagram of the relay and the associated circuits. The purpose of the circuits is to excite the flux-shifting coil on the relay at the proper time. The timing is effected as follows:

(*a*). A synchronous motor running at 1,800 rpm turns three coaxial disks at 600 rpm through a 3 to 1 gear train.

(*b*). One disk has a shorting bar on the periphery which closes the bias circuit of the *FG* 17 thyratron. The thyratron plate current excites the flux-shifting coil on the relay, causing it to close. This coil excitation occurs once every six cycles. The part of the 60-cycle wave at which it occurs is determined by the space angle of the synchronous motor rotor. This space angle can be varied through 360 degrees by means of the phase shifter on the input.

(*c*). A cam is set on the end of the synchronous motor shaft so that it opens the relay about four cycles (1/15 second) after the closing instant.

(*d*). Another disk on the motor shaft is arranged to break the thyratron plate circuit after the arc in the tube has been initiated. This is necessary to make the tube ready for the next closure.

(*e*). A third disk on the motor shaft has a shorting bar on the periphery. This is oriented so that during the two cycles after the cam opens the relay, the charge on the capacitors in the circuit may be reduced to zero.

The oscillogram, Figure 8, shows the performance of the timed switch. Note that each cycle of operation is repeated in every detail. The result is a steady image of the first four cycles of current in the circuit on an oscilloscope screen.

The *FG* 17 circuit is switched in if it is desired to keep the circuit closed after a given closure. This circuit is used in conjunction with an oscillograph to get photographs of the current, and capacitor voltage wave forms from the time of closure until the steady state is reached.

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Study of Driven Rods and Counterpoise Wires in High-Resistance Soil on Consumers Power Company 140-Kv System

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THE Consumers Power Company operates an extensive 140-kv system in eastern, southern, and western Michigan. In parts the top soil is sand, sometimes extending to a considerable depth, and tower footings in this soil have very high resistance compared with resistance encountered in other soils.

In 1936 the Consumers Power Company and the General Electric Company began a joint investigation of the ground-circuit arrangement best suited to carry the lightning current with a minimum potential across the line insulators. The test setup was arranged to study the relative merits of the driven rod, the continuous-parallel counterpoise, and the radial counterpoise, as well as to investigate the current and voltage conditions near the surface of the earth in the vicinity of the lightning stroke.

Methods of Grounding

To study various methods of grounding, a portion of the *N*-14 line between Croton and Muskegon was divided into three sections, and each section was equipped with special earth connections as follows:

Section *A*. Towers 7,659 to 7,682, parallel continuous counterpoise and right-angle counterpoise.

Section *B*. Towers 7,683 to 7,708, parallel continuous counterpoise and driven rods.

Section *C*. Towers 7,711 to 7,723, right-angle counterpoise and driven rods. (Tower 7,709 has been removed. Tower 7,710 is equipped with ground rods, but not with right-angle counterpoise because of deep gully and road.)

Figures 1a, 1b, and 1c show the three arrangements. The length of each section is approximately two miles. The counterpoise wire consists of seven-strand $^3/_8$-inch galvanized-steel cable buried to a depth of approximately 18 inches. The parallel wire extends continuously between towers, passing directly beneath the towers and connecting to the middle leg. The right-angle counterpoise wires extend 250 feet each side of the line and are attached to the side legs of the towers. The total length for each tower approximates the length of one span.

The rods were driven as deep as necessary to secure a resistance of between 10 and 20 ohms. In some cases one rod was sufficient and in other cases two rods were necessary. The rods were made of $^5/_8$-inch copper bearing steel, 8 feet long, the lengths being joined together by pressed fit couplings and driven into the ground by gasoline hammers.[1,2] Rods were driven to all depths, up to 150 feet in some cases.

In addition to the counterpoise wires and driven rods connected to the towers, 10 isolated probe wires approximately 100 feet long were buried in the ground, beginning approximately 300 feet from the line and extending at right angles to the line. These were spaced approximately uniformly throughout the experimental region between towers 7,659 and 7,723.

The *N*-14 line for the greater part of its length is a single-circuit line on steel towers. The conductors are arranged in a triangle, two on one side of the tower and one on the other, the equivalent delta spacing being approximately 15.6 feet. The total length of the line between Croton and Muskegon is 37.4 miles, of which 35.4 miles is 3/0 steel-reinforced aluminum cable and two miles 4/0 steel-reinforced aluminum cable. There is a ground wire at the peak of the towers made of three strands of number 7 Copperweld. The line is insulated with 9 disks spaced $5^3/_4$ inches on suspension and 11 disks on dead ends.

The towers are of the three-legged type and each leg was equipped with one

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The authors acknowledge the assistance of many people in this investigation, especially J. R. Eaton, now of Purdue University, who had active charge for Consumers Power Company of the installation of the driven rods, counterpoise wires, and measuring equipment; Carl C. Tanner, who carried on the field work after Mr. Eaton left the company; and R. F. McAtee of the general engineering laboratory, General Electric Company, who helped in the calibration work and in analyzing the data.

Table I. Footing Resistance of Towers of the *N*-14 Line

Section and Arrangement	Tower No.	Footing Resistance (Ohms)	No. of Driven Rods	Depth of Driven Rods (Feet)	Combined Resistance, Footing and Rods (Ohms)
Section *A*, parallel and right-angle counterpoise	7,659	460			
	7,660	510			
	7,661	660			
	7,662	750			
	7,663	680			
	7,664	750			
	7,665	640			
	7,666	600			
	7,667	840			
	7,668	360			
	7,669	420			
	7,670	700			
	7,671	730			
	7,672	820			
	7,673	740			
	7,674	750			
	7,675	360			
	7,676	920			
	7,677	860			
	7,678	760			
	7,679	740			
	7,680	920			
	7,681	620			
	7,682	610			
Section *B*, parallel counterpoise and driven rods	7,683	620	1	100	14
	7,684	840	1	82	15
	7,685	970	1	92	14
	7,686	520	1	76	9.5
	7,687	720	2	48–52	24
	7,688	590	2	32–36	19
	7,689	810	1	53	15
	7,690	770	2	42–41	14
	7,691	490	1	56	14
	7,692	440	1	58	10.5
	7,693	800	1	100	8.5
	7,694	675	1	100	8.5
	7,695	910	1	124	11
	7,696	775	1	90	13
	7,697	700	1	94	16
	7,698	775	1	116	11
	7,699	630	1	100	10.5
	7,700	6 0	1	108	10
	7,701	690	1	92	14
	7,702	680	2	58–57	14
	7,703	650	1	84	11.5
	7,704	640	1	84	10
	7,705	540	2	65–53	23
	7,706	825	1	108	10
	7,707	560	2	71–92	9
	7,708	1,400	1	92	11
Section *C*, right-angle counterpoise and driven rods	7,710*	850	2	121–92	13
	7,711	665	2	108–104	12
	7,712	910	2	98–108	13
	7,713	800	1	81	12.5
	7,714	1,030	1	100	8.5
	7,715	1,050	1	128	11
	7,716	850	1	132	14
	7,717	650	1	124	14
	7,718	810	1	156	12
	7,719	880	1	100	8
	7,720	610	2	84–100	7
	7,721	890	1	124	10
	7,722	700	1	116	7
	7,723	493	1	140	12

There is no tower 7,709.

* Tower 7,710 has two driven rods, but no counterpoise connected to it.

bracket for magnetic links. After the 1937 season, brackets were installed on the overhead ground wire on each side of every tower, about 3 feet away from the tower. The continuous counterpoise wires were equipped with brackets on each side of each tower, about 4 feet from the center tower leg, and also at the middle of each span. The right-angle counterpoise wires on each side of the tower were equipped with one bracket at 4 feet from the tower leg, and on only the south side one at 125 feet and one at 225 feet. Each separate probe wire had a bracket at approximately the middle of its length. Each driven rod was equipped with one bracket. In all cases each bracket was equipped with two magnetic links in order to obtain a current range and to make use of the oscillatory calibration.[3]

The entire test portion of the *N*-14 line extends over a very uniform flat sandy plain. Table I gives the resistance measured by ground megger of the footing of each tower of the *N*-14 line included in this investigation. These resistances are of the footings alone and were measured before counterpoise wires and driven rods were installed. The table also gives for sections *B* and *C* the lengths of the driven rods and the final combined resistance of tower footing and driven rods.

During the years 1937 to 1941 inclusive, six strokes were experienced on the experimental part of the *N*-14 line. Of these one was in section *A*, two in section *B*, and three in section *C*. Following is a brief analysis of the important features of these strokes:

Table II. Footing Resistance of Towers on the *T*-20 Line

Tower No.	No. of Driven Rods	Combined Resistance Footing and Rods (Ohms)
4,387	4	125
4,388	4	21
4,389	4	28.5
4,390	2	25
4,391	2	29
4,392	2	22
4,393	2	33
4,394	2	35
4,395	1	51
4,396	1	56
4,397		760
4,398		897
4,399		729
4,400		1,299
4,401		942
4,402		1,240
4,403		1,260
4,404		816
4,405		1,187
4,406		784
4,407		266
4,408		75
4,409		138
4,410		15
4,411		14
4,412		13
4,413		158
4,414		434
4,415		131
4,416		44
4,417		—

Stroke 1

On October 19, 1937, there was a stroke to the overhead ground wire of the *N*-14 line, between towers 7,764 and 7,765, in section *A*, equipped with parallel counterpoise and right-angle counterpoise. Current records were obtained at 14 towers, 7,659 to 7,672 inclusive. The stroke was negative, that is, all tower currents flowed upward and all parallel counterpoise currents toward tower 7,665 and all right-angle counterpoise currents toward the towers. An idea of the comparative ability of the two types of counterpoise to pick up current may be gained from the following:

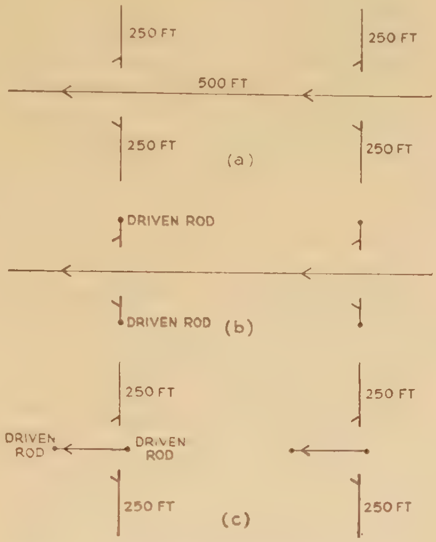

Figure 1. Experimental installation of parallel continuous counterpoise, right-angle counterpoise, and driven rods

For the parallel counterpoise, the difference between the current in a span approaching the tower and the current in the same span leaving the preceding tower is taken as the current collection or pickup in that span. For the right-angle counterpoise, the currents approaching the tower at each side at the magnet station nearest the tower are added up to give the pickup for that tower. Table III shows the parallel counterpoise pickup and Table IV the right-angle counterpoise pickup for this stroke. It will be noted that these currents are nearly equal, 23,000 amperes total in the parallel counterpoise and 25,700 amperes in the right-angle counterpoise.

At the first tower at which readings were obtained, tower 7,659, there were 2,800 amperes in the parallel counterpoise at the stroke side of the tower and no current was read in the counterpoise on the other side of the tower, in the tower legs, or in the right-angle counterpoise. Likewise, at the extreme end, tower 7,672, there were 1,800 amperes in the parallel counterpoise on the stroke side and no current was read elsewhere. The origin of this current is unknown.

It is interesting to note that current flowed in the parallel counterpoise for about six spans on each side of the point of stroke, while the right-angle counterpoise carried readable current at only three towers adjacent to the stroke, and the tower members themselves showed current in only two towers. This wide extent of current pickup on the parallel continuous counterpoise is common on this line, and also has been observed on other lines.[4,5]

Table III. Parallel-Counterpoise Current Pickup, Stroke 1

Toward Croton: Tower No.	Amperes	Toward Muskegon: Tower No.	Amperes
Beyond 7,659	0	Beyond 7,672	0
7,659 to 7,660	700	7,672 to 7,671	200
7,660 to 7,661	500	6,671 to 7,670	500
7,661 to 7,662	1,000	7,670 to 7,669	600
7,662 to 7,663	2,200	7,669 to 7,668	500
7,663 to 7,664	3,100	7,668 to 7,667	2,100
7,664 to 7,665	4,300	7,667 to 7,666	2,300
		7,666 to 7,665	5,000
	11,800		11,200
			11,800
Total counterpoise pickup			23,000

Table IV. Right-Angle Counterpoise Current, Stroke 1

Tower No.	Direction of Counterpoise Wire	Amperes
7,664	North	2,500
	South	2,500
7,665	North	8,000
	South	8,300
7,666	North	2,200
	South	2,200
Total		25,700

Isolated probe conductors were installed in the ground opposite towers 7,660 and 7,667, but no current was measured in these wires. However, neither was any current measured in the right-angle counterpoise wires extending out toward the probe conductors at these towers.

It is interesting to obtain an idea of the comparative ability for collecting current of the shallow buried counterpoise wires

and the short tower footings, extending perhaps eight feet below the ground surface. This may be done as follows:

Table V shows the total current picked up by the ground system, exclusive of the tower footings, which could not be measured in these tests.

Current was measured in only two towers, 7,664 and 7,665, as shown in Table VI. The difference between the total tower current of 58,400 amperes and the total counterpoise pickup current 53,300 amperes is 5,100 amperes, which is assumed to be the tower-footing pickup current.

If the current of 2,800 amperes in counterpoise at tower 7,659 and 1,800 amperes in counterpoise at tower 7,672 is prorated to the parallel and right-angle counterpoise in the ratio 23,000 to 25,700, we can sum up the distribution of currents between counterpoise and tower footings and evaluate the counterpoise resistance.

Table V. Measured Current Picked up by Counterpoise System, Stroke 1

	Amperes
Parallel counterpoise	23,000
Right-angle counterpoise	25,700
Current in counterpoise at tower 7,659	2,800
Current in counter poise at tower 7,672	1,800
Total	53,300

The multiple resistance of the 14 towers, 7,659 to 7,672, from Table I, is 42.7 ohms. The counterpoise resistance is then assumed to be inversely proportional to the current pickup, as shown in Table VII.

This comparison indicates that the counterpoise wires are very efficient compared with the natural tower footings which penetrate only a short distance below the ground in the same top soil occupied by the counterpoise wires.

Stroke 2

On August 20, 1938, there was a stroke to the overhead ground wire on the *N*-14 line between towers 7,708 and 7,710. Tower 7,709 had been removed prior to

Table VI. Tower Current, Stroke 1

Tower No.	Amperes Leg 1	Amperes Leg 2	Amperes Leg 3	Total Current (Amperes)
7,664	8,000	1,600	1,600	11,200
7,665	11,700	8,500	4,000	24,200
				35,400
Estimated cross-member current 65 per cent				23,000
Total				58,400

Table VII. Counterpoise and Tower-Footing Current and Resistance, Stroke 1

	Current Amperes	Current Per Cent	Resistance (Ohms)
Parallel counterpoise	25,170	43.2	8.66
Right-angle counterpoise	28,130	48.3	7.75
Tower footings	5,100	8.5	42.7
Total	58,400	100.0	

this time, so that towers 7,708 and 7,710 were adjacent. The stroke was negative, that is, cloud negative and earth positive, all tower currents flowing upward and all counterpoise currents flowing inward toward the point of stroke.

Tower 7,708 was at one end of section *B*, equipped with parallel counterpoise and driven ground rods. Tower 7,711 was at the beginning of section *C*, equipped with right-angle counterpoise and driven ground rods, but tower 7,710 had only two driven rods and no counterpoise connected. The span between towers 7,708 and 7,710 was long and ran across a gully.

Table VIII summarizes the pickup in the counterpoise wires and driven rods. Approximately one half of the total current picked up by the driven rods was collected by the single driven rod at tower 7,708. This rod was 92 feet deep and had a measured resistance of approximately 11 ohms. Tower 7,710 had two driven rods, one 121 feet deep and one 92 feet deep, with a combined measured resistance of about 13 ohms. These two rods, however, picked up only 4,200 amperes total. It appears that ground currents flowing in the gully at right angles to the line were tapped more readily by the single rod at tower 7,708, possibly because of soil resistance in the vicinity and also possibly because the cloud may have been centered more to the north of the gully (toward tower 7,705).

The set of readings in the parallel-counterpoise and driven-rod section, towers 7,705 to 7,708 inclusive, lends

Table VIII. Counterpoise and Driven-Rod Current, Stroke 2

Tower No.	Counterpoise Type	Counterpoise Amperes	Driven Rod (Amperes)
7,705	Parallel	1,000	1,000
7,706	Parallel	800	2,200
7,707	Parallel	200	4,400
7,708	Parallel	2,500	13,400
7,710*			4,200
7,711	Right angle	2,000	2,000
Total		6,500	27,200

* Not installed.

Table IX. Driven-Rod, Counterpoise, and Tower-Footing Current and Resistance, Stroke 2

	Current Amperes	Current Per Cent	Resistance (Ohms)
Driven rods	21,000	81.1	2.93
Parallel counterpoise	4,500	17.3	13.7
Tower footings	340	1.6	179.0
Total	25,840	100.0	

itself to a determination of the equivalent resistance of the counterpoise, on the assumption that the resistances are inversely as the currents carried. The tower-footing resistance for towers 7,705 to 7,708 inclusive, in multiple, is found to be 179 ohms, from Table I, the combination tower and driven-rod resistance is found to be 2.89 ohms, and by calculation the driven-rod resistance alone is 2.93 ohms. Table IX shows that the driven rods carried about 81 per cent of the total current and the parallel continuous counterpoise about 17 per cent, with the tower-footing current negligible. The equivalent resistance of the parallel counterpoise is 13.7 ohms, compared with 2.9 ohms for the driven rods.

A buried probe conductor opposite tower 7,707 carried no measurable current, while the driven rods at this tower picked up 4,400 amperes.

Stroke 3

This stroke took place on July 30, 1940, to the overhead ground wire between towers 7,714 and 7,715, in section *C*, equipped with right-angle counterpoise and driven rods. The stroke was negative. All tower currents flowed upward and all rod and counterpoise currents flowed toward the towers. Table X summarizes the current collection by right-angle counterpoise and driven rods.

The multiple footing resistance of the 12 towers, 7,712 to 7,723 inclusive, from

Table X. Right-Angle Counterpoise and Driven-Rod Current, Stroke 3

Tower No.	Counterpoise (Amperes)	Driven Rod (Amperes)
7,712	2,000	1,800
7,713	2,500	6,300
7,714	8,900	12,000
7,715	10,900	30,000
7,716	6,000	6,600
7,717	2,000	2,500
7,718	2,500	1,800
7,719	2,000	1,600
7,720	2,000	1,000
7,721	2,000	1,500
7,722	2,000	1,000
7,723	3,100	1,000
Total	45,900	67,100

Table XI. Driven-Rod, Counterpoise, and Tower-Footing Current and Resistance, Stroke 3

	Current		Resistance (Ohms)
	Amperes	Per Cent	
Driven rods	67,100	59.0	0.86
Right-angle counterpoise	45,900	40.3	1.26
Tower footings	900	0.7	64.0
Total	113,900	100.0	

Table I, is 64 ohms, while the multiple resistance of the combined footings and ground rods is 0.85 ohm. From these figures the driven-rod resistance alone calculates 0.86 ohm.

Table XI shows the division of current and the equivalent resistance of the driven rods, right-angle counterpoise, and tower footings, on the assumption that the resistances are inversely proportional to the currents. From Table XI it will be seen that the driven rods carry about 59 per cent of the current and the right-angle counterpoise wires about 40 per cent, the tower-footing current being negligible.

From Table XI the summation of the currents picked up by the driven rods, right-angle counterpoise wires, and tower footings is about 114,000 amperes. The sum of all tower currents involved in the stroke, towers 7,712 to 7,723 inclusive, is 60,700 amperes, which increased by 65 per cent for estimated cross member currents, would give 100,000 amperes. This should be increased by 3,300 amperes coming in on the overhead ground wire from beyond tower 7,712. Thus there is good agreement between the current of about 114,000 amperes collected from the ground and the tower current of about 103,000 amperes. However, the current in the overhead ground wire at the two ends of the span that was struck totaled only 57,300 amperes. This current is undoubtedly too low. A saturated inner link at the Croton side, at tower 7,715, permitted the use of the outer link only, with the unidirectional calibration curve. This value is therefore a minimum. This discrepancy might indicate that the counterpoise and ground-rod current crests are not coincident. The currents in the counterpoise in the ground surface might rise to crest value more quickly than the currents in the ground rods, which are draining a charge from 100 feet below the surface, including the ground charge extending well out beyond the surface counterpoise wires.

It will be noted from Table X that, in the towers near the lightning stroke, the driven-rod currents are much greater than the counterpoise currents, while at the more remote towers the pickup is more nearly equal, if not greater in the counterpoise. This might be expected if the major portion of the ground charge is located deeply and flows through a conducting layer radially to the driven rods near the lightning stroke.

Summation of the counterpoise currents to the north of the line gave 30,000 amperes, while those to the south of the line totaled 15,900 amperes. This would suggest that the cloud charge participating in the lightning stroke was more to the north of the line.

A reading of 5,000 amperes was obtained in a buried probe conductor opposite tower 7,715, while in the three stations on the right-angle counterpoise wire, extending in the same direction, readings were obtained of 1,000, 2,500, and 3,400 amperes, going progressively toward the tower. Also a reading of 1,800 amperes was obtained in a probe conductor opposite tower 7,722, while a reading of 1,000 amperes was obtained in the right-angle counterpoise extending in the same direction at the magnet station nearest the tower.

Stroke 4

This stroke took place July 30, 1940, to the overhead ground wire of the *N*-14 line between towers 7,702 and 7,703, in section *B*, with parallel continuous counterpoise and driven rods.

The current collection for the parallel counterpoise and driven rods is shown in Table XII. The stroke was negative.

The resistance of the footings of towers 7,700 to 7,706 inclusive, in multiple, is 93.7 ohms from Table I. The combined resistance of tower footings and driven rods is 1.74 ohms and the calculated resistance of the driven rods alone is 1.77 ohms.

Table XIII shows the distribution of current among the driven rods, parallel counterpoise, and tower footings, and the equivalent resistance on the basis that the current divides inversely as the resistance. It will be noted that the driven rods carry 88 per cent of the total current, as against 10 per cent for the parallel counterpoise wires.

Table XII. Parallel-Counterpoise and Driven-Rod Current, Stroke 4

Counterpoise		Driven Rods	
Tower No.	Amperes	Tower No.	Amperes
Beyond 7,700	1,600	7,700	1,900
7,700 to 7,701	0	7,701	2,300
7,701 to 7,702	1,100	7,702	10,100
7,702 to 7,703	2,300	7,703	17,800
		7,704	7,100
		7,705	1,000
		7,706	2,000
Total	5,000		42,200

Table XIII. Driven-Rod, Counterpoise, and Tower-Footing Current and Resistance, Stroke 4

	Current		Resistance (Ohms)
	Amperes	Per Cent	
Driven rods	42,200	88.0	1.77
Parallel counterpoise	5,000	10.4	15.0
Tower footings	800	1.6	93.7
Total	48,000	100.0	

Buried probe conductors opposite towers 7,700 and 7,707 showed no readable current. The driven rods at tower 7,700 picked up 1,900 amperes and there were readings in the parallel counterpoise wire and in the tower legs at this tower. However, at tower 7,707 no readings were obtained in the driven rods, counterpoise wire, or tower legs.

Strokes 5 and 6

These strokes occurred on October 2 1941, to the overhead ground wire between towers 7,715 and 7,716, and between towers 7,720 and 7,721. This is the section of line equipped with right-angle counterpoise and driven rods (section *C*). Subsequent inspection showed flash marks on all three insulator assemblies at tower 7,721, but there was no tripout on this date. Both strokes were negative.

The current collection for the right-angle counterpoise and driven rods is shown in Table XIV.

The multiple resistance of the footings of the ten towers, 7,714 to 7,723 inclusive, from Table I is 76 ohms, and the multiple resistance of the combined driven rods and tower footings is 0.976 ohm. From these figures the resistance of the driven rods alone is 0.99 ohm. The distribution of current and the equivalent resistances are shown in Table XV. It will be noted that the driven rods carry

Table XIV. Right-Angle Counterpoise and Driven-Rod Current, Strokes 5 and 6

Tower No.	Counterpoise (Amperes)	Driven Rod (Amperes)
7,714	0	1,500
7,715	6,400	1,000
7,716	0	1,800
7,717	0	3,200
7,718	0	3,000
7,719	0	10,400
7,720	7,300	40,600
7,721	14,000	42,000
7,722	0	15,000
7,723	0	6,000
Total	27,700	124,500

Table XV. Driven-Rod, Counterpoise, and Tower-Footing Current and Resistance, Strokes 5 and 6

	Current: Amperes	Current: Per Cent	Resistance (Ohms)
Driven rods	124,500	81	0.99
Right-angle counterpoise	27,700	18	4.45
Tower footings	1,620	1	76.0
Total	153,820	100	

about 81 per cent of the current, as against 18 per cent for the right-angle counterpoise.

No readable current was measured in buried probe conductors opposite towers 7,715 and 7,721. In the right-angle counterpoise wire, extending toward the probe wire at tower 7,715, 6,400 amperes were measured at the magnet station adjacent to the tower. In the right-angle counterpoise wire, extending toward the probe wire from tower 7,721, currents of 800, 3,600, and 5,800 amperes were read progressively toward the tower.

Voltage Gradient in Vicinity of Towers

For the purpose of indicating the voltage conditions in the vicinity of the tower during lightning strokes, a portion of the *T*-20 line was selected. This line runs between Croton and Grand Rapids, a distance of approximately 39.2 miles. The line has four-legged double-circuit steel towers, with the conductors arranged vertically on one side of the tower only for 32 of the 39.2 miles. The conductors are 110,000-circular-mil copper, with 15.1-feet equivalent delta spacing. One overhead ground wire, consisting of three strands of number 8 Copperweld, is installed at the peak. The line is insulated with ten disks, spaced $4^3/_4$ inches on suspension, and 12 units on dead ends. A section of this line from towers 4,387 to 4,417 inclusive, 31 towers, was selected for this investigation.

About two thirds of this test section traversed a sandy plain where tower-footing resistance was very high, while the remainder passed over low ground composed of muck and sand where footing resistances were comparatively low. Ten consecutive towers in the high-resistance section, 4,387 to 4,396 inclusive, were equipped with deep-driven rods to reduce the tower-footing resistance. In two cases there was one rod per tower, in five cases two rods per tower, and in three cases four rods per tower. The resistances of all the towers in the experimental section are given in Table II.

At one side of each tower, and about six feet from the tower legs, a probe rod was driven into the ground about five feet deep, and between the tower and the probe rod was introduced a lightning-stroke recorder.[6] The purpose of this recorder was to measure the voltage drop between the tower and the probe rod during lightning strokes.

Each tower leg was equipped with a bracket, and after 1937 a bracket was installed on the overhead ground wire on each side of each tower. Each deep-driven rod was also equipped with a bracket. In all cases each bracket was equipped with two magnetic links.

Eight strokes occurred on the *T*-20 line during the period of this investigation (1936 to 1941 inclusive). Six strokes in which there was good correlation between the tower current and the potential between tower and probe rod are summarized in Table XVI.

The stroke on September 15, 1936 (stroke 7) made contact with the overhead ground wire between towers 4,404 and 4,405. Measurements were obtained at 31 towers extending from 4,387 to 4,417 inclusive. At 29 towers, records were obtained on the lightning-stroke recorders connected between the tower leg and the five-foot probe rod. These readings ranged from 5 to about 50 kv, the external flashover value of the recorder. There were 3 cases of external flashover and 13 cases of film flashover. As a result of this experience, capacitance dividers with ratios of 2 to 1 were installed at all tower probe-rod stations.

On June 24, 1937, a stroke occurred to tower 4,408 (stroke 8). Records were obtained at 19 towers, 4,395 to 4,413 inclusive. In 7 cases the lightning-stroke recorder flashed over. For the other 12 cases, the readings ranged from 9 to about 100 kv, which was the flashover of the recorder and voltage-divider combination. It was therefore necessary again to increase the voltage-divider rati and a 6 to 1 ratio was installed. Thi ratio proved sufficient, as no more flash overs were experienced.

All the strokes of which there were goo records were negative. Stroke 12 i worthy of mention. This stroke (no listed in Table XVI) took place to the to of tower 4,389. About 6,300 ampere flowed upward in this tower, and a total o 6,300 amperes was measured in the tw deep-driven rods connected to this tower flowing upwards. In the adjacent towe 4,388, no current was measured, but i the second tower to the north, 4,387 about 13,200 amperes flowed downwar and 6,000 amperes was measured flowin downward in the single deep-driven ro connected to this tower. Currents fro the overhead ground wire flowed towar the stroke from beyond tower 4,387 an from beyond tower 4,391 to the south.

Surge Resistance of Tower Footings Under Natural Lightning Conditions

The potentials measured on the prob rods driven five feet deep at a distanc six feet from the tower footings have bee correlated with the tower-structure currents as shown in columns 4 and 6 o Table XVI. These tower-to-probe voltage measurements of column 6 are les than true tower potential by the eart potential differences beyond the prob rods. An approximation of the relatio between measured tower-to-probe-ro voltage and actual tower potential i found as follows:

The current from the earth to th tower footing is assumed to be distribute uniformly in the earth, its density bein the same at identical distances from th tower footing and inversely proportiona to the square of the distance from th footing. The earth's resistance, bein proportional to cross section, also is de-

Table XVI

(1) Stroke No.	(2) Tower No.	(3) R_N Tower-Footing Resistance (Ohms)	(4) I_T Total Tower Current† (Amperes)	(5) $R_N I_T$ Tower Potential (3)×(4) (Kv)	(6) V_M Tower-to-Probe Potential (Kv)	(7) V_T Tower Potential (6)×2.5 (Kv)	(8) R_S Surge Resistance (7)/(4) (Ohms)	(9) R_S/R_N (8)/(3) Ratio
7	4,394	35	6,000	210	27	67.5	11.2	0.32
8	4,410	15	3,300	50	5	12.5	3.8	0.254
9	4,408	75	11,700	876	190	475	40.6	0.542
10	4,400*	1,299	13,700	17,800	280	700	51.0	0.039
	4,401	942	6,800	6,400	170	425	62.5	0.066
11	4,394	35	6,600	230	40	100	15.1	0.432
	4,395	51	16,000	816	170	425	26.4	0.517
	4,396	56	11,000	617	140	350	31.8	0.569
13	4,412	13	45,600	592	222	555	12.2	0.939

* Flashover all three phases.

† Total tower current (column 4) assumed 1.5 times measured current in tower legs, to take care of cross member current.

creasing as the square of the distance from the tower footing.

The voltage gradient at ground points of differing distances from the footing will be inversely as the square of the tower to point distance. The potential at a ground point is the integrated voltage gradient and is therefore inversely proportional to distance.

Accordingly, with an assumed tower-base spread of 18 feet and a probe point 6 feet farther out, the probe potential will be to the tower potential as 9 is to 15, or the tower potential will be 15/9 times the probe potential. The tower potential less the measured tower-to-probe voltage will be the probe potential. Therefore, the measured tower-to-probe potential must be multiplied by 2.5 to give tower potential. Values of true tower potential calculated on this basis are given in column 7.

To show the relation between the surge resistance and the normal footing resistance, the values of column 9 were calculated by dividing values in column 8 by those in column 3. These figures show the surge resistance to range from some 4 to 90 per cent of normal resistance values. These values are somewhat lower than those reported from artificial surge measurements.[7,8] In this connection, however, it should be observed that the range of normal resistances, from 13 to 1,300 ohms, is wider than is ordinarily investigated, and the range of currents, 6,000 to 45,000 amperes, is also very wide. In general in these measurements we are dealing with high resistances and large currents.

The resistance ratio of column 9 was shown, by plotting, to decrease very definitely with increasing currents and with increasing resistances. Accordingly, this ratio was plotted against a composite factor the product of up-tower current and normal footing resistance shown in column 5. In Figure 2 the ratio of surge resistance to normal resistance of column 9 is plotted as ordinate against the product of current and normal footing resistance of column 5. The points, while somewhat scattered, nevertheless show very clearly the trend indicated by the solid line. This is borne out by plotting on log-log paper, which shows a relation between ratio and product as follows:

$$\frac{R_S}{R_N}=\frac{14}{(R_N I_T+80)^{0.6}}$$

in which R_S=surge resistance, R_N=normal or megger tower-footing resistance, and I_T=total tower current.

For practical purposes these curves might be utilized somewhat as follows: Assume that it is desired to evaluate the surge resistance of a certain tower under a lightning current of 10,000 amperes, where a normal tower-footing resistance of 100 ohms has been measured. The product of tower current and footing resistance is $1{,}000 \times 10^3$, which referred to the curve gives a ratio of 0.22. This ratio applied to the 100-ohms footing resistance gives a surge resistance of 22 ohms. The resultant tower potential would be 220,000 volts, which is substantially below flashover.

The flashover of this line is taken as 700 kv on a 1.5x40-microsecond wave and on this basis it is interesting to examine actual field operating records as to insulator-assembly flashovers that might coordinate with the derived tower potential of column 7. Flashover occurred at tower 4,400 for stroke 10, for which the derived tower potential is just 700 kv. All three phases flashed over. No other flashovers are correlated with tower-to-probe measurements for the records shown in this table. Accordingly, on the basis of this flashover it is indicated that the ratio between true tower potential and measured tower-to-probe potential could not be lower than the 2.5 figure derived from the assumption outlined above. Also for tower 4,412, stroke 13, the tower potential is 555 kv, as the last item in column 7. In this case there was no flashover and the tower-to-probe potential was 222 kv. The ratio of 700 to 222 kv, or slightly over 3 to 1, is therefore indicated as the upper limit by these data. It is therefore concluded that the true ratio is between 2.5 and 3 to 1 and the use of the curve for interpreting true surge resistances from tower currents and normal ground-resistance measurements is seen to be reasonable.

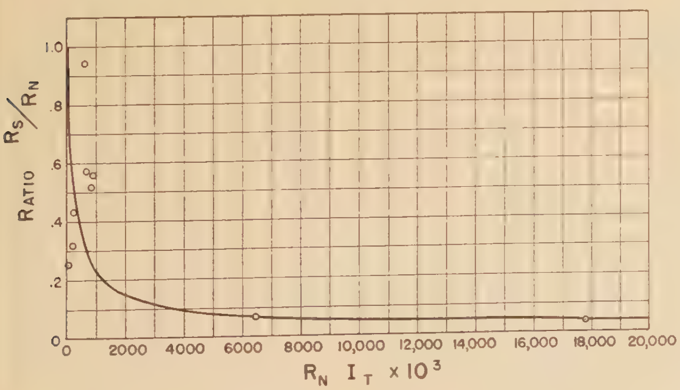

Figure 2. Ratio of surge resistance to normal tower-footing resistance, plotted against product of current and normal footing resistance during natural lightning strokes

Conclusions

In field lightning research, the investigators are often disappointed at the few direct strokes in the experimental section, even after a period of several years. The present investigation is no exception. However, it is believed that the preceding analysis and the following conclusions represent a fair interpretation of the limited data available. It should be understood that the conclusions apply specifically to the conditions prevailing on this particular transmission line.

1. Parallel continuous counterpoise wires and right-angle counterpoise wires of equal total length have approximately equal current-collecting ability. Direction of counterpoise apparently is not a governing factor, but only resistance to ground. In comparison with the normal tower footings the counterpoise wires are very effective in picking up lightning current.

2. In sandy high-resistance soil, rods driven deep enough to penetrate the clay or loam beneath the sand serve to reduce the tower-footing resistance from 500–1,400 ohms to 7–25 ohms. These rods, in comparison with counterpoise wires in the high-resistance surface soil, are much more effective in picking up lightning current.

3. There is some indication that the current pickup in the counterpoise wire near the ground surface and in the deep-driven rods is not coincident. Possibly the ground rod requires more time to discharge the surrounding earth on account of its deep penetration into the earth.

4. Data seem to indicate that the ratio of surge tower-footing resistance to normal tower-footing resistance decreases definitely with increasing currents and with increasing resistances. For example, at 10,000 amperes and 100 ohms the surge resistance decreases to approximately 22 ohms. This may account for frequent failure to flash over when the product of current and normal resistance indicated that flashover should take place.

5. The ratio of surge resistance to normal footing resistance is shown under natural lightning conditions to be as low as 0.04 for sandy soil, where normal resistance ranges up to 1,000 ohms or more and tower current up to approximately 50,000 amperes.

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Precision Speed Control for World's Largest Induction Motor

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A RECENT paper by Dickey, Kilgore, and Laffoon described an installation consisting of a 40,000-horsepower wound rotor induction motor and the necessary auxiliary equipment required to provide for variable speed operation.[1]

To meet the many requirements of control, imposed by this application, it was necessary to devise new methods for insuring smooth acceleration of auxiliaries and main motor, for limiting the change in power required from the supply system to a definite maximum value, to provide precision regulation of speed, as well as means for control for the main motor from either of two points. In addition, power-factor regulation and means for emergency quick stopping was provided. These and many other features were incorporated in the automatic control, the functions of which are briefly outlined in the following paragraphs.

In order that the auxiliary motor generator sets and main motor may be supplied with adequate bearing lubrication and ventilation at all speeds, it is necessary to start the oil circulating pump and ventilating fans prior to placing those units in service. In addition, the seven-unit exciter set, which supplies d-c energy to the fields of the machines comprising the auxiliary motor-generator sets, must be started. After these preliminary operations are complete, the starting indication is given the automatic control by manual operation of the starting control switch which energizes the master relay. Starting and acceleration of the auxiliary motor-generator sets and connection of the main motor to the 6,900-volt bus is accomplished in the following sequence:

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1. Closing of d-c machine field breakers.
2. Closing of high-voltage breaker for constant speed motor-generator set, energizing motor through starting reactor.
3. Closing of starting reactor short-circuiting breaker.
4. Energizing of fields of a-c machines.
5. Connection of speed matcher to bus and primary of main motor.
6. Acceleration of variable speed motor-generator set under control of constant current regulator for d-c machine fields and master speed-control rheostat operated by speed matcher contacts.
7. Energizing of voltage-balancing relay and automatic synchronizer.
8. Closing of main motor primary breaker.

At this point the main auxiliaries are operating at synchronous speed and the main motor is at rest but connected to the 6,900-volt three-phase bus. To start the main motor the operator, at one of the two suitably interlocked control points, turns the pointer on the speed-control dial to a definite speed position. Following that manual operation the automatic control completes the following operations:

1. Starts rotor-lift oil pumps for main motor.
2. Causes the variable speed set to slowly decrease in speed.
3. Main motor begins to rotate and gradually approach the desired speed.
4. Acceleration stops and main motor speed is maintained constant by means of the electronic speed regulator, controlled from a pilot generator on main motor. This regulator provides energy for speed-regulating field windings on d-c machines of constant speed motor-generator set.

In stopping the main and auxiliary units, the automatic operations take place in the reverse order to those performed when starting. The same smooth operation is afforded, as in starting, with power being fed back into the system as a means of decelerating the machines.

All of the recent developments in switching equipment were employed to provide utility, flexibility, and compactness of design. The 6,900-volt switchgear is of the metal-clad type, using oil circuit breakers having a suitable interrupting capacity; all low-voltage circuits are protected with metal-enclosed air circuit breakers, and the main control switchboard is of duplex construction. In addition to all those features, the latest innovation in relay construction was employed by the use of plug-in devices for both protection and automatic control.

Starting of Auxiliaries

All of the power and switching equipment, consisting of the following apparatus, is located in the power building (Figure 2).

1. 40,000-horsepower main motor.
2. 6,250-kva constant-speed motor-generator set.
3. 38,000-horsepower variable-speed motor-generator set.
4. 400-horsepower seven-unit exciter set.
5. 6,900-volt metal-clad high-voltage switchgear.
6. 460-volt metal-enclosed switchgear.
7. Seven-panel main duplex control switchboard.
8. Ventilation blowers for main motor and each motor-generator set.
9. Lubricating-oil circulating equipment.

Apparatus for remote control of the main-motor speed and for emergency quick stopping is installed on a control desk in the control room of the test chamber, Figure 3. The test chamber is an integral part of the wind tunnel.

If it is assumed that test operations are to be conducted, the first requirement is that the operator in the power building start all equipment necessary for supplying energy to the wind tunnel. This operation is initiated from the main control switchboard by first starting the exciter set, the ventilating fans, and lubricating equipment by remote manual push-button control. When these auxiliaries are in service, the main auxiliaries may be

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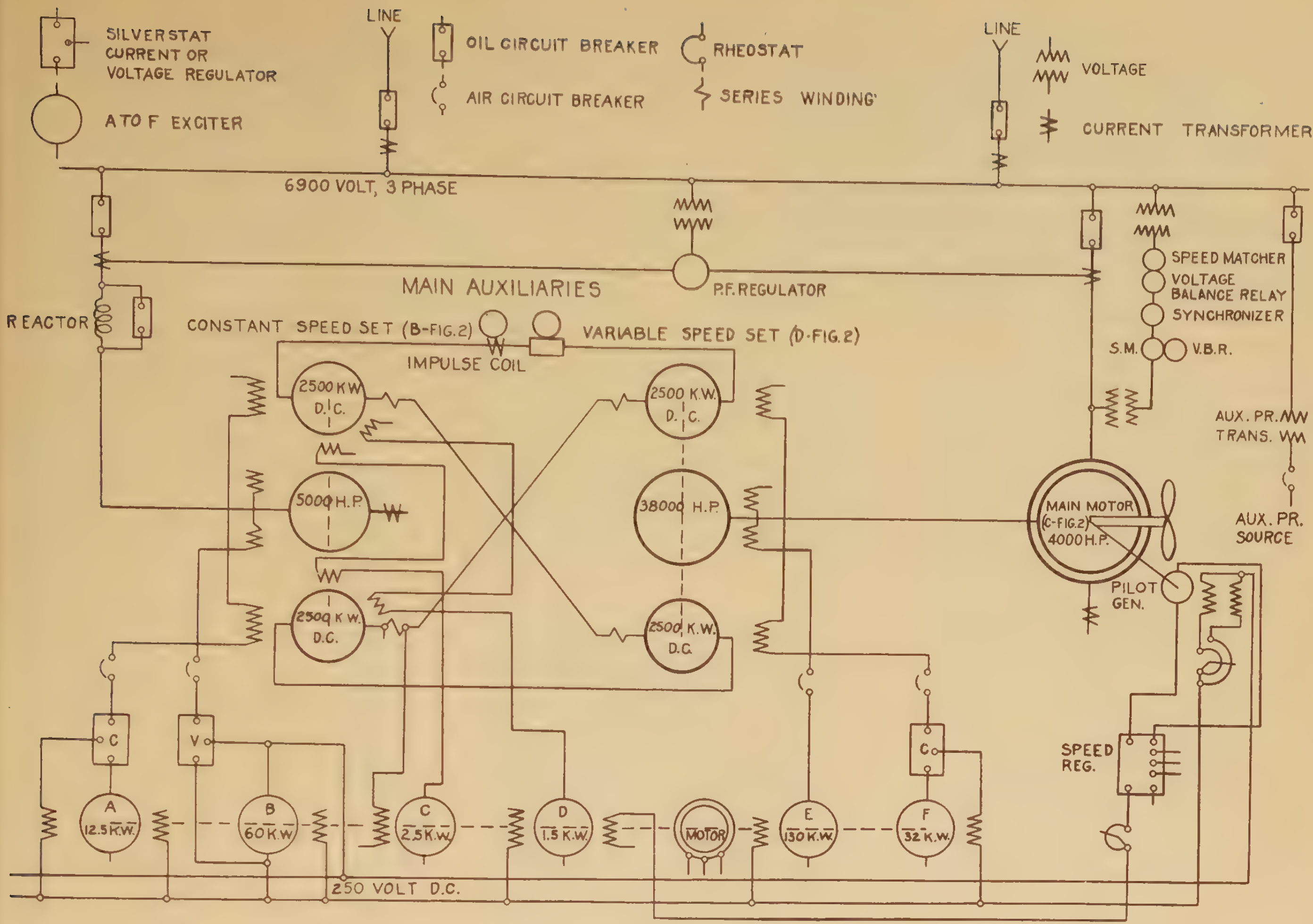

Figure 1. Schematic connection diagram for 40,000-horsepower motor and auxiliaries

started by means of a control switch on the main control switchboard. This switch energizes the master relay of the automatic control and all further starting operations are accomplished under the supervision of that equipment.

Starting of Constant-Speed Motor-Generator Set

The constant-speed motor-generator set consists of a 6,250-kva a-c machine and two 2,500-kw 750-volt d-c machines. During the starting operation of both main auxiliary sets, the a-c machine functions as a synchronous motor, and the d-c machines as generators. After the 40,000-horsepower motor is in service, the d-c machines operate as motors driving the a-c machine as a synchronous generator supplying power to the a-c system. Because of the dual function of these machines and to avoid confusion, they are referred to simply as a-c and d-c machines rather than as motors or generators.

Each operation, in the starting scheme, follows a planned automatic sequence which insures correct and positive functioning of the equipment being controlled. Following energizing of the master relay, the automatic control closes the field breakers for the fields of the d-c machines of both main auxiliary sets. Completion of this operation, as indicated by interlocks on the field breakers and a contact on the field relay for the d-c machines of the variable-speed set, the a-c machine of the constant-speed set is connected to the 6,900-volt bus through a starting reactor.

During the time that the constant-speed motor-generator set is accelerating, excitation on the exciter for the d-c machines of this set is held at zero. After sufficient time has elapsed for the set to reach approximately synchronous speed, a reactor short-circuiting breaker is closed connecting the a-c machine directly to the bus. This operation is followed by application of field energy, and the motor-generator set then pulls into synchronism and is ready to assume the load required for starting the variable-speed unit.

Master Speed-Control Rheostat

Because of the necessity for simultaneous control of all d-c-machine fields when initiating speed changes, it was necessary to combine all field rheostats in a motor-driven master speed-control assembly. This master rheostat is comprised not only of rheostats for exciter field control, but rheostats for recalibrating the Silverstat constant current regulators used in the fields of the d-c machines of the main auxiliary sets. In order to assure correct commutation on the d-c machines of the variable-speed set at all loads, shunt rheostats connected across those commutating fields are included in the master assembly. Also, this assembly contains a recalibrating rheostat for the electronic speed regulator, as well as one for controlling the field current to the driving motor of the master speed-control rheostat. This latter rheostat is used to provide for slow operation of the entire assembly when the variable-speed set or main motor is being accelerated, or decelerated, thereby assuring a slow and uniform change in power on the system.

Starting of Variable-Speed Motor-Generator Set

The variable-speed motor-generator set consists of a 38,000-horsepower synchronous machine and two 4,000-horse-

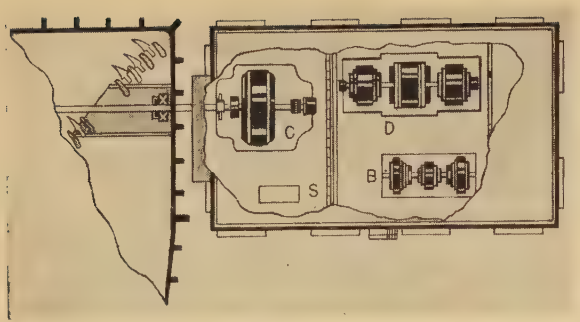

Figure 2. Automatic-control switchboard "S", main motor "C", variable-speed set "D", and constant-speed set "B"

power 750-volt d-c machines. The a-c machine is designed for a stator current of approximately 5,000 amperes at 5.5 cycles frequency when operating at approximately 48 rpm. This machine will also operate at a stator voltage of 4,800 volts at 60 cycles frequency and when rotating at 514 rpm. During the starting period the a-c machine operates as a synchronous generator supplying exciting current to the windings of the 40,000-horsepower main-motor rotor to which it 's connected. When the main motor is in service, the a-c machine of the variable-speed set functions as a synchronous motor driven by the power generated in the rotor of the main motor—due to the slip frequency when the motor is rotating at other than synchronous speed. When the variable-speed set is being started, the d-c machines act as motors driven by energy from the d-c machines on the constant-speed set. After the main motor is in operation, these same d-c machines function as generators, driven mechanically by the a-c machine, and supply power to drive the constant-speed motor-generator set which feeds power into the a-c system. Had the conventional means of speed control been used, this power would have been dissipated as heat and been lost.

During the starting operation of the variable-speed set, the master speed-control rheostat is under control of a balanced torque-type speed matcher. The elements of the speed matcher are connected to potential transformers on either side of the open contacts of the main-motor primary breaker. Initially with the variable-speed set at rest, there is no voltage on the stator or primary terminals of the main motor, and the speed-matcher element connected to the bus side of the breaker operates to close a contact to cause the master speed-control rheostat to operate. The rheostat will function to permit excitation of the fields of the d-c machines of the constant-speed set, and those machines will deliver energy to the d-c machines on the variable-speed set. As this set begins to rotate, the a-c machine will generate a voltage and frequency proportional to the speed. An equivalent voltage at equal frequency will appear on the stator terminals of the main motor, due to the transformer action of the motor windings. So long as the frequency of the voltage on the motor terminals is below that of the voltage on the bus, the speed matcher, operating through the speed-control rheostat, will cause the variable-speed set to accelerate. Acceleration is accomplished first, by increasing the field strength on the d-c machines of the constant-speed set to a maximum and, finally, by decreasing the field current in the d-c machines of the variable-speed set until synchronous speed is reached.

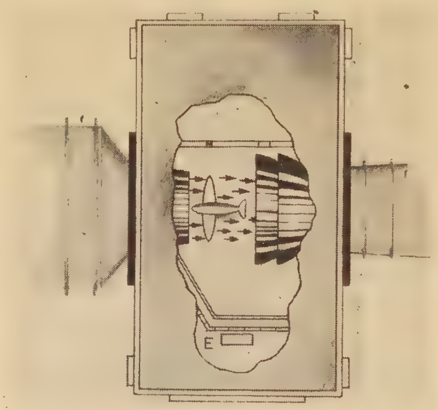

Figure 3. Remote-control desk "E"

Connection of Main, 40,000-Horsepower Motor to Bus

In order that power drawn from the power-supply system may be a minimum when the main motor is connected to the bus, the voltage on the stator windings is synchronized with the bus voltage before the oil circuit breaker is closed. This synchronizing operation is accomplished by means of an electronic-type automatic synchronizer operating in conjunction with the speed matcher and a voltage-balancing relay. At the instant that synchronism is accomplished, the oil circuit breaker is closed automatically.

All operations of the speed matcher, synchronizer, and voltage-balancing relay cease with closing of the main-motor breaker, and these devices are automatically removed from the circuit. However, a power-factor regulator takes over control of the excitation of the a-c machine of the variable-speed set, and through operation of a motor-operated shunt field rheostat functions to maintain practically unit power factor during the time the main motor is connected to the bus. Except for power-factor control the automatic equipment performs no further operations until the indication is given manually to start the main motor. Both auxiliary motor-generator sets continue to rotate at synchronous speed, since no device is yet in operation to change the position of the master speed-control rheostat, and the main motor remains at rest while the frequency of the rotor voltage is in synchronism with that applied to the stator.

Starting of Main Motor

Normally, control of the main motor speed is the duty of the operator in the test chamber, and for this reason, a speed-control dial, Figure 4, equipped with contacts and a synchro-tie motor, is mounted on the test-chamber control desk. A similar dial is located on the main switchboard, but it may be assumed that speed control from that point will be an emergency operation in the event that a fault occurs in the control cable between the power building and the test chamber. Control assignment switches are located beside each control dial for the purpose of assigning control to either, but not both, locations. Assignment of the point of control is accomplished by both assignment switches being in agreement, and, after once being assigned, cannot be changed by either one of the operators

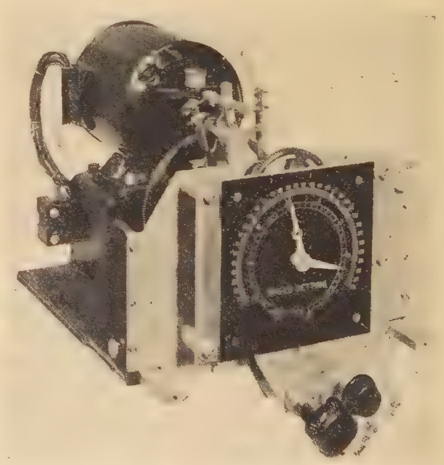

Figure 4. Speed-control dial, contact mechanism, and Synchro-tie

changing the position of his switch. A set of indicating lamp signals, coupled with the assignment switches, indicate the point to which the control has been assigned, and also indicate when the assignment switches are not in agreement.

With the main motor connected to the bus, but at rest, the operator to whom control is assigned may cause the motor to start and assume a certain definite speed by merely turning the knob on the control dial until the pointer reaches the desired speed position. It is well to note that the automatic control is provided with a position indicating relay, so that if the speed-control dial is not at the zero speed position when the main motor is connected to the bus, it will be necessary for the operator to return the pointer to zero and then to the desired speed setting before the motor will start. This insures that the motor will remain at reset after being connected to the bus during the starting operation, even though the control dial is at other than the zero speed position. Movement of the pointer off the "zero" speed position will cause the starting of rotor-lift oil pumps which will raise the main-motor rotor off the bearings, and when this is accomplished, the speed control equipment will operate to start the motor.

Acceleration of the main motor is accomplished by gradually slowing down the variable-speed set by first strengthening the field of the d-c machines on that set, and, as the speed decreases, by weakening the fields of the d-c machines on the constant-speed set. These field changes are accomplished by means of the master speed-control rheostat. This rheostat is equipped with a synchro-tie motor which is coupled electrically to a similar motor on the speed-control dial mechanism. When rheostat and dial mechanism are in the same relative position, with respect to main-motor speed setting, contacts on the dial mechanism are closed, short-circuiting the operating coils of the polarized "raise–lower" relay which is a part of the speed-control apparatus. Moving the dial mechanism to some fixed speed position opens these contacts, and the polarized control relay operates to cause the master rheostat to operate in a direction which will increase or decrease the main-motor speed, depending upon which way the dial pointer is turned. As the rheostat moves to accomplish the desired speed change, the synchro-tie moves a contact on the dial mechanism. When the rheostat reaches the position which will cause the motor to operate at the new speed setting, the control contacts will again close, and the rheostat will stop.

Control of Motor Speed

After the master speed-control rheostat comes to rest, the control for constant speed of the main motor is taken over by an electronic speed regulator which receives control energy from a pilot generator geared to the motor shaft. This speed regulator is recalibrated for any new speed setting by means of a rheostat on the master speed-control rheostat assembly. The regulator energizes the field of a small 1.5 kw speed-control exciter which supplies energy to field windings on the constant-speed machine. By increasing or decreasing the shunt field strength of this machine, the speed of the main motor is held constant by varying the torque through control of the power taken from the rotor and fed back into the a-c system.

By varying the speed of the speed-control rheostat automatically, smooth acceleration and deceleration of the main motor is possible. Also, by this means, the amount of power drawn from the bus during acceleration may be held to a steadily increasing value not exceeding a fixed increase per minute at any point on the speed range between 0 and 295 rpm. When the desired speed is reached, the electronic regulator maintains that speed constant within a range of 0.5 per cent for speeds from 37.5 rpm to 150 rpm and 0.3 per cent between 150 and 295 rpm.

Stopping of Main Motor and Motor-Generator Sets

The normal method for stopping the main motor and auxiliaries is first to reduce the main-motor speed to zero, then trip the motor breaker by turning the "start–stop" control switch to the "stop" position. Tripping of this breaker will disconnect the motor from the bus, and the automatic control will operate to decelerate the variable-speed set and cause it to come to rest. When this set is at rest, the high-voltage breaker for the power supply to the constant speed a-c machine will be tripped, and all automatic equipment will assume a normally de-energized position in readiness to again start the units at the will of the operator.

Emergency Quick Stopping

Means is provided for rapid deceleration of the main motor in case of emergency and, under such conditions, the motor is decelerated at the most rapid possible rate permitted by the power company. Rapid deceleration will cause power to be fed into the power company's system by regeneration through the main auxiliary motor-generator sets. Means is also provided for emergency shutdown and disconnection of all units from the bus should the necessity arise. Under this condition, all high-voltage and field circuit breakers are tripped simultaneously, and the main motor and motor-generator sets drift to standstill with friction and windage losses being the only decelerating forces.

Protective Devices

A complete complement of protective relays is provided with the automatic control. Those devices which prevent starting or operate an annunciator and sound an alarm are as follows:

1. Failure of bearing oil flow.
2. Failure of ventilation air.
3. Low lubricating oil pressure.
4. Low a-c voltage.

Those devices which merely annunciate and sound an alarm are:

1. High machine winding temperature; all machines.
2. High lubricating oil temperature.
3. Low lubricating oil temperature.
4. High bearing temperature.
5. Basement air temperature.
6. Low lubricating oil level.
7. Ground on auxiliary power circuits.

Devices which operate a lockout relay, drop an annunciator, sound an alarm, and cause all machines to be immediately removed from service are:

1. Overspeeding of either main auxiliary motor-generator set.
2. Unbalanced phase current on constant-speed a-c machine or main motor.
3. Differential protection on constant speed a-c machine or main motor.
4. A-c overcurrent in constant speed a-c machine or main motor.
5. Low a-c voltage on the bus.
6. Failure of unit to complete automatic starting sequence.
7. Overcurrent on d-c machines of main auxiliaries.
8. Short circuit on d-c machines of main auxiliaries.
9. Manual operation of emergency stopping switch.

The more serious of these faults, as indicated, cause immediate disconnection of the units from the bus while others merely sound an alarm and warn the operator that continued operation is undesirable, at which time provisions can be made to complete the test in as short a time as

Electrical Features of Design and Operation of the Plantation Pipe Line

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PLANTATION Pipe Line Company's new 1,254-mile pipe line system is the largest ever built for the handling of refined petroleum products and is the largest all-electric-powered pipe line in existence. First full-length delivery was made early in February 1942, and immediately the line became an important factor in the transportation of gasoline and fuel oils to a large area in the southeastern states. Compared to the usual pipe line, this system operates under highly exacting conditions, and in its design the electrical and associated control features are of prime importance. The major electrification problems were:

1. To design for economical future increase in line capacity.

2. To provide flexibility in operation to meet varied schedule requirements.

3. To co-ordinate pumps, motors, and control, so as to accomplish the starting of units reliably and with reasonable demand on the station power service.

4. To design stations for safe handling of petroleum products.

5. To develop a station control system, co-ordinating the electric and hydraulic-control devices and meeting the requirements of single-attendant operation.

General Conditions of Operation

The statements made in this report regarding the capacities, pressures, and horsepowers, both for present operation and future installation possibilities, do not necessarily represent actual conditions, but do indicate the general principles involved.

The system comprises an unloading dock at the supply terminal with a short line of four parallel 12-inch pipes to a storage tank farm and the initial station of the pipe line proper, a section of 12-inch line, followed by a section of 10-inch line extending to the delivery terminal. Four branch lines serve areas to the north and south of the main line. The tankage at the supply terminal main line station is used to store incoming shipments, and to make up pipe-line shipments (known as tenders) according to the system schedule. Smaller storage facilities are located at the branch line origins.

Approximately 30 delivery terminals are situated at various points along the system, each with facilities for handling several products. The handling of multiple products to 30 delivery points creates a scheduling and dispatching problem probably far more complex than on any existing line.

The capacity of the line as initially equipped is 60,000 barrels per day of gasoline through the 12-inch section and 42,000 barrels per day through the 10-inch section. This requires two 600-horsepower motor-driven main-line pumps for each of the seven stations on the 12-inch line, and two 450-horsepower units in each of the seven stations on the 10-inch line. Each of three branch lines is served by a single 150-horsepower unit. A 450-horsepower unit is required at the origin of the fourth branch line, plus an additional booster station requiring a single 450-horsepower unit. The unloading station at the supply terminal dock contains five 600-horsepower units, providing ample capacity for rapidly unloading two tankers simultaneously. The initial mainline station at the supply terminal tank farm contains a complete spare pump unit to insure the availability of two units at all times. The system comprises 16 stations with a total of 39 centrifugal pumping units exclusive of tankage pumps and auxiliary pumps.

Provisions for Future Increase in Capacity

The present installation is designed as a long-term project to cover the commer-

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possible. All of the above protective features may be incorporated in the control of different types of machines but, never before, have all been included in the protection of a single assembly of machines.

Switchgear

The high-voltage metal-clad switchgear is equipped with lift-type oil circuit breakers which may be easily removed for inspection and service. Low-voltage metal-enclosed switchgear makes use of drawout-type air circuit breakers of suitable interrupting capacity for the circuits involved. The control switchboard is of the duplex type with meters and control switches on the front panels and all automatic and protective relays on the rear panels. Meters are semiflush, and all relays are semiflush of the plug-in type, equipped with stab contacts which permit easy removal from the panel for inspection and calibration. The entire assembly is easily accessible for maintenance and service, and all parts are of most advanced design with formed steel switchboard panels assembled in a complete self-supporting structure.

Conclusion

The automatic control provides reliable means, by use of proved apparatus, for performing all of the necessary starting, stopping, speed control, and protective functions for equipment of the type and size employed. The various switchgear assemblies are compact and well adapted to the service.

Many new switching problems were encountered in designing the complete control, and their solution opens up new fields of application to other apparatus. In addition, different types of standard equipment were employed to provide a complete and modern design which can be easily duplicated and one in which spare or renewal parts are readily obtainable.

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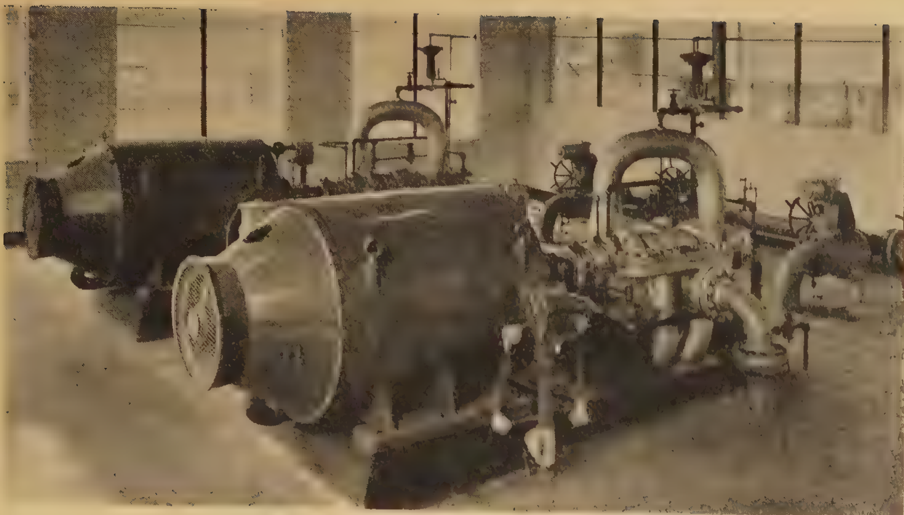

Figure 1. Pump room of typical main-line station on Plantation Pipe Line system

cial needs of the territory served and to operate economically on that basis. At the same time provision is made for possible future operation at increased capacity.

The maximum pressure at which the line will be operated is approximately 900 pounds per square inch in the 12-inch section and 975 pounds per square inch in the remaining sections. These values are determined by the allowable unit stress in the pipe walls, which in turn is a matter of economics in the design of the line. It is necessary in centrifugal pump operation that positive suction pressure be maintained at each pump, and if the minimum allowable suction pressure at the initial pump is established as, say, 40 pounds per square inch, then with a maximum allowable station discharge pressure of 900 pounds per square inch, the pressure increment introduced into the line at any station is 860 pounds per square inch, plus the pressure loss through the station piping. The stations are so spaced, then, that the pressure drop between stations due to fluid friction at rated line throughput, plus the static pressure differential totals approximately 860 pounds per square inch. Between the discharge of one station and the suction of the next, with uniform pipe, there is a uniform decrease in the component of total pressure due to line friction, this pressure drop being directly proportional to the distance from the upstream station. Referring to Figure 2, which represents a level section of 12-inch line, *SA* represents the suction pressure at initial station *A*, *DA* the discharge pressure at this station, and line *DA-SB* the pressure gradient along some 60 miles of line from station *A* to station *B*, for a flow rate of 60,000 barrels of gasoline per day. This is the flow that can be carried by the line under the conditions prescribed. If station *B* is at an elevation higher than station *A*, the distance between stations must be decreased by an amount that will reduce the friction drop sufficiently to provide the pressure differential necessary to balance the positive static head, and if station *B* is lower than station *A*, the distance between them will be increased to compensate for the negative static head.

To increase the capacity of the line without exceeding the maximum allowable working pressure, a second series of intermediate pressure increments may be added, one of which is represented by intermediate station *M*. If this station is placed at the hydraulic mid-point between stations *A* and *B*, the same pressure increment may be introduced at station *M* as at station *A*. The flow rate then will increase to a value such that the pressure drop between stations *M* and *B* will be the same as existed between stations *A* and *B* before adding the pressure increment at *M*. For the line in question this flow rate for gasoline is calculated at approximately 90,000 barrels per day.

When this higher flow rate is established, obviously the horsepower input per station must be increased in both the initial and added stations. Whereas the initial stations are equipped with two 600-horsepower units for 60,000 barrels per day throughput, operation at 90,000 barrels per day will require two 900-horsepower units in all stations.

All stations on the main line are arranged for future change-over to higher capacity by initially installing pumps of such design that new impellers can be readily substituted, and motors designed for ultimate operation at the increased horsepower. These motors are of the totally enclosed fan-cooled type, and in order to secure efficient operation of the 12-inch line motors at 600 horsepower, they are equipped initially with a ventilating fan designed to deliver only the amount of ventilating air required by the motor operating at this capacity; when the change-over to 900 horsepower is made, no electrical or mechanical changes are involved except replacement of this one fan on the front shaft extension. Similarly, on the 10-inch section of the line, motors of ultimate 600 horsepower capacity are installed with initial fan capacity for operation at 450 horsepower. The motor-control and power-supply equipment in all the main-line booster stations are designed for operation at the ultimate ratings.

Operating Flexibility

All motors 75 horsepower and above are 2,300 volt. All motors above 150 horsepower are 3,600 rpm. All motors 450 horsepower and above are mechanically interchangeable, simplifying the problem of spare equipment, because a spare motor of 600/900-horsepower design can be installed on any line pump unit in the system except the 150-horsepower branch-line units. This feature also provides maximum utility of the equipment under unpredictable future alterations, due to changed operating conditions.

While the line is designed for a certain rated capacity, consideration must be given also to its flexible and economical operation at reduced throughputs. Contrary to practice on lines handling one uniform type of tender, the throughput of this line must be held above a certain minimum rate, to prevent excessive contamination between adjoining tenders

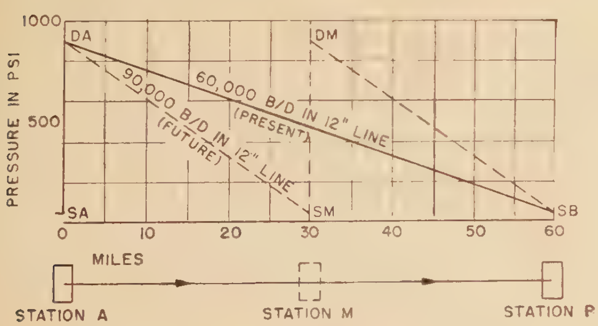

Figure 2. Pipe-line pressure gradients for flows of 60,000 barrels per day and 90,000 barrels per day

passing through the line. Between this rate and the full capacity of the line it is desirable, from the standpoint of power consumption, to operate at a rate as low as will suffice to meet the transportation schedules.

Motors of the constant-speed type are preferred because of their simplicity and their adaptability to construction suitable for hazardous atmospheres. Installation of two units per station, operated at constant speed, with the pumps connected in series, provides the required degree of flexibility. With this arrangement, and with all stations along the line having piping installed to permit by-passing any unused unit or station, changes in throughput in steps of a few per cent can be made by operating 2, 1, or 0 units in properly selected stations.

Motor-Starting Conditions

With the station locations determined solely by the hydraulics of the line and, therefore, without regard to existing power facilities, it was necessary to consider carefully the method of starting the units, from the standpoint of voltage conditions on the electric service.

Most stations contain two main pumps comprising 95 per cent of the station load. From the standpoint of starting conditions the 12-inch line motors are 900 horsepower, and the 10-inch line motors 600 horsepower. The motors are of low-starting torque design in order to minimize rotor resistance and attain high operating efficiency, important in view of the high load factor at which the system will operate. To minimize current peaks during starting, motor starters for all units 450 horsepower and above are of the autotransformer type, with closed-circuit transition. During the brief interval when the motor is transferred to full voltage with its terminals still connected to the starting tap, a reactor is inserted between the autotransformer tap and the motor to limit the current in the autotransformer circuit. By suitable control this reactor also provides an additional reduced-voltage starting step when power is first applied to the motor.

During starting, the discharge valve of the centrifugal pump is closed which minimizes the required motor torque. After the unit is up to speed the discharge valve is opened slowly so that the change in direction of flow through the station piping will be gradual, for well-known hydraulic reasons. Speed-torque curves for the pump with closed discharge, and for the motor with various applied voltages, are shown in Figure 3. These curves apply to the 12-inch line pumps when equipped with impellers for 900-horsepower operation, representing the most difficult conditions that must be met. The speed-torque requirements of the pumps are shown as a band curve, the upper limit applying to the pump when operating on fuel oil of specific gravity 0.83 and the lower limit for gasoline of 0.73 specific gravity.

Motor speed-torque curves and line current-torque curves are shown for full voltage, also for 80 per cent voltage applied by autotransformer. Also shown are speed-current curves for three-step starting with the reactor initially connected between the autotransformer tap and the motor. With the reactor in circuit approximately 85 per cent of the tap voltage appears at the motor terminals during the first portion of the initial starting step.

During starting, the voltage actually applied to the motor terminals should be of sufficient value to enable the motor to develop the torque necessary to reliably accelerate the unit to a speed above pull-out on the starting tap; when this speed is exceeded, the current decreases rapidly, and transfer to full voltage can be made on a low-current portion of the motor current-torque curve. Acceleration occurs directly as the excess of motor torque beyond pump input torque and inversely as the total inertia of the unit. Starting must be accomplished within a reasonable time, a limit in the order of 30 seconds being established as desirable by considerations of heating of the fluid within the pump casing. To perform the accelerating duty within this time limit it is found that the speed-torque curve of the motor when operating from the autotransformer tap should not fall below the curve corresponding to an applied voltage of about 1,630 volts. This means, when accelerating with the motor on tap giving a voltage of 80 per cent of line voltage, and with the reactor shunted, the voltage supply to high-voltage side of the starting autotransformer must be not less than:

1,630 volts ÷ 0.80 = 2,038 volts, or 88.6 per cent of normal 2,300 volts

Using a starting tap giving 85 per cent of line voltage, the voltage supply to the starting equipment must be not less than:

1,630 volts ÷ 0.85 = 1,918 volts or 83.4 per cent of normal 2,300 volts

To accommodate both present and future pump requirements, as well as variations in voltage conditions encountered at the several stations, the starters for the 600/900-horsepower motors were equipped with autotransformers having taps to deliver, under starting load, 80, 85, or 90 per cent of line voltage. In the first portion of the initial starting step, when the reactor is inserted into the tap circuit, the voltage at the motor terminals for these three tap connections is respec-

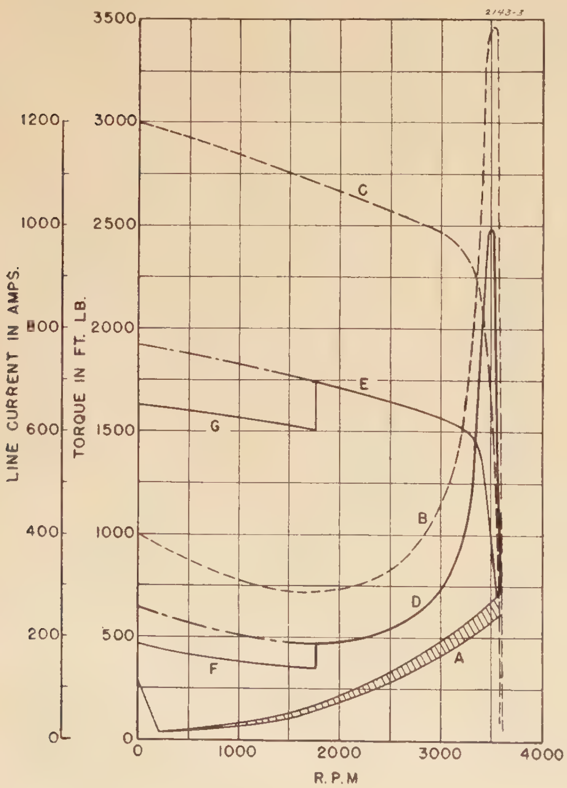

Figure 3. Starting characteristics of motor-driven pump on 12-inch line

A—Speed-torque for pump starting with closed discharge

B—Speed-torque for motor starting on 100 per cent voltage

C—Speed-line current for motor starting on 100 per cent voltage

D—Speed-torque for motor starting on 80 per cent voltage autotransformer tap

E—Speed-line current for motor starting on 80 per cent voltage autotransformer tap

F—Speed-torque for motor starting on 80 per cent voltage autotransformer tap with series reactor

G—Speed-line current for motor starting on 80 per cent voltage autotransformer tap with series reactor

tively, about 68, 72, or 76 per cent of normal line voltage.

Station Design as Affected by Hazardous Nature of Tenders

Safety problems introduced by the handling of petroleum hydrocarbon products of volatile and inflammable nature required special consideration throughout the design of the stations.

The areas subject to the occurrence of hazardous atmospheres (class *I*, group *D* as defined in the National Electrical Code) are the pump room and adjoining piping platform. The pump room contains the pumps with associated valves, hydraulic control devices, and piping. On the piping platform are situated the incoming and outgoing line piping and all necessary station and auxiliary piping with associated scraper traps, strainers, filters, valves, a sump tank with motor-driven pump for its service, and in many stations pressure-reducing valves and fluid meter and meter calibration facilities for delivery of product from the line to the distribution terminals.

The pump room is ventilated by a flow of incoming fresh air through glass-wool filters below the windows in the front wall of the station and directly opposite the motor air intakes. This air is forced through the motors by the motor blowers and discharged toward the rear wall of the pump room. In the rear wall just below the ceiling are mounted one or more motor-driven exhaust fans which discharge the air from the pump room and onto the piping platform. The piping platform is open on three sides with a flat roof for weather shelter, a construction affording maximum natural ventilation. The piping platform and pump room are at ground level, and in their design trenches and other depressions favorable to accumulating hydrocarbon vapors were avoided.

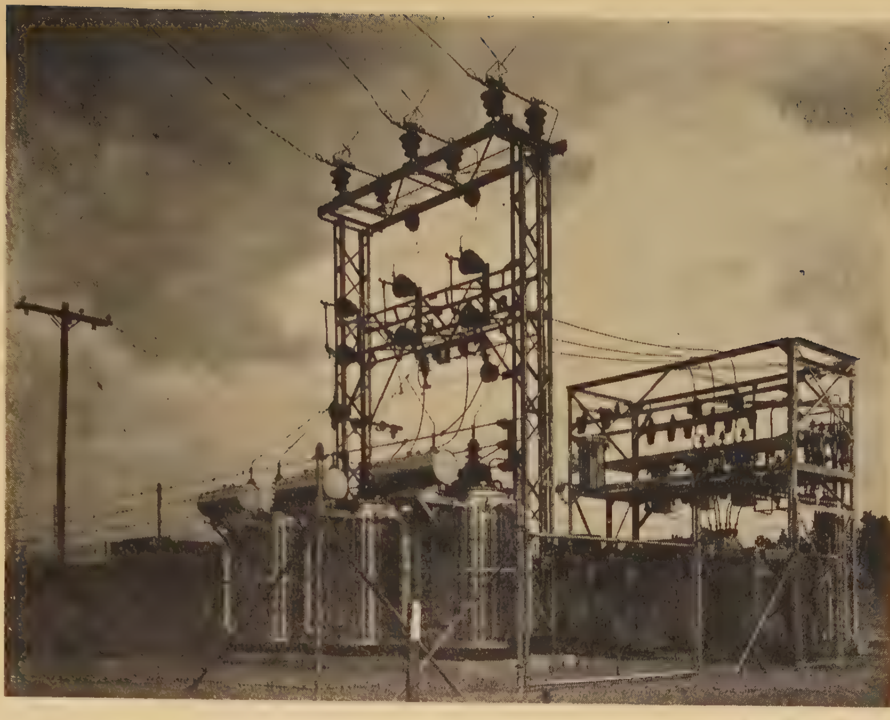

Figure 5. Outdoor substation for pumping station serving main and two branch lines

44- to 2.3-kv transformer bank consists of three 833-kva units. At right is the outdoor breaker controlling the 2,300-volt underground circuit to the pumping station bus. Structure at right mounts transformers for lighting, outside auxiliaries, and metering, as well as low-voltage breakers for lighting and outside auxiliary circuits

All electric motors, control, signal devices, motor-operated valves, lighting fixtures, and wiring installation in this area are specifically designed for service in class *I* group *D* hazardous atmospheres.

The 600/900-horsepower motors on the 12-inch line pumps are believed to be the largest, explosion-resisting motors ever built. Attainment of this rating in a relatively compact construction—an important consideration in explosion-resisting design—is accomplished by the circulation of large volumes of cooling air through the motor frame and by the use of class *B* insulation with the motor rating based on 75 degrees centigrade rise. All main-line pump motors have sleeve bearings, oil-ring lubricated and cooled by air blast from the motor ventilating fan. These bearings require no oil circulating system; consequently each motor is an entirely self-contained unit without external accessories.

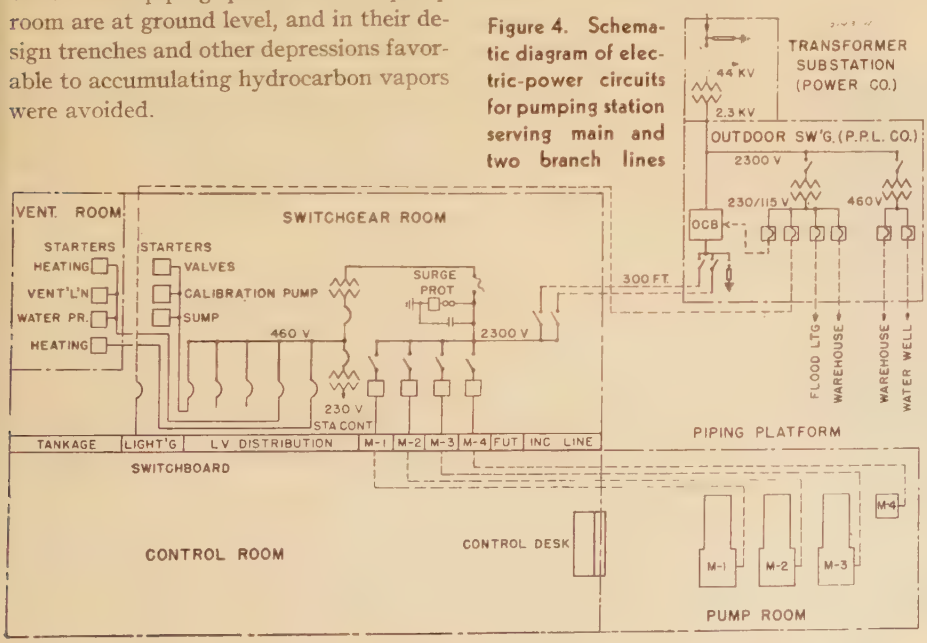

Figure 4. Schematic diagram of electric-power circuits for pumping station serving main and two branch lines

Control apparatus is of dead-front construction. With appropriate measures taken for safety, experience in products pipe-line operation indicates that provision of a nonhazardous location and installation therein of switchgear and control apparatus of open design are preferable to the use of special forms of switchgear and control in explosion-resisting or oil-immersed enclosures designed for class *I* group *D* location. This is true from both cost and operating standpoints. The importance of accessibility hardly can be overemphasized; since the several stations on a pipe line operate in series and interdependently, it is imperative that during operation of the line any inspection and maintenance work on electrical equipment be performed without shutdown or in emergency with the very minimum of shutdown time.

Nonhazardous conditions are maintained in all parts of the pumping station

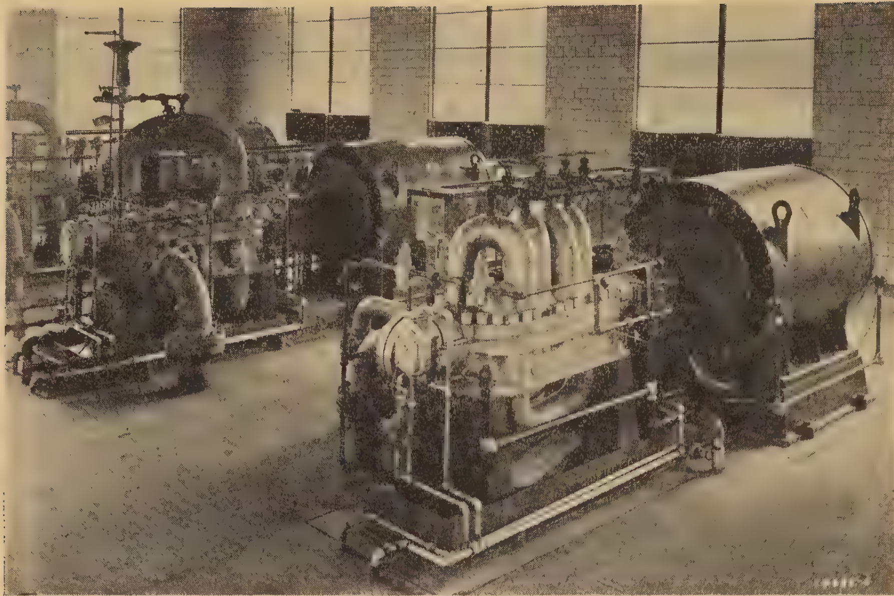

Figure 6. General view of pump room showing two main-line pumps (left) and branch-line pump (right) driven by 450/600-horsepower explosion-resisting motors

building, except the pump room and piping platform. The nonhazardous portion of the building, designated as the operating section, has a floor level about three feet higher than the pump-room floor and is completely separated by wall from the pump room and piping platform. No direct connection is provided between these areas, it being necessary to pass through outside entrances in going from one to the other. The atmosphere in the operating section is maintained in safe condition by outside air drawn into the building at the end opposite the pump room, maintained at a pressure a few inches above the outside air and above the atmosphere in the pump room. This insures that any air seepage through the wall, separating the operation section from the pump room or piping platform, will be in the direction to prevent entrance of hazardous atmosphere to the operating section.

The operating section of the station comprises a control room, switchgear room, ventilating and auxiliary room, lavatory, and in three of the stations a division superintendent's office.

The wall between the control room and the pump room consists of a vapor-tight, wire-glassed observation window. Into the center section of this window is sealed a vapor-tight steel gauge board, on which are mounted hydraulic recording and control devices and indicating pressure gauges as will be discussed later. These devices are gasketed to prevent passage of vapor through the mounting openings.

To provide for safely shutting down the station in case the control room should become hazardous due to an equipment failure, an explosion-resisting "emergency" pushbutton is mounted on the control desk contiguous to the gauge board. By pushing this button the attendant will trip the circuit breaker at the origin of the incoming power feeder.

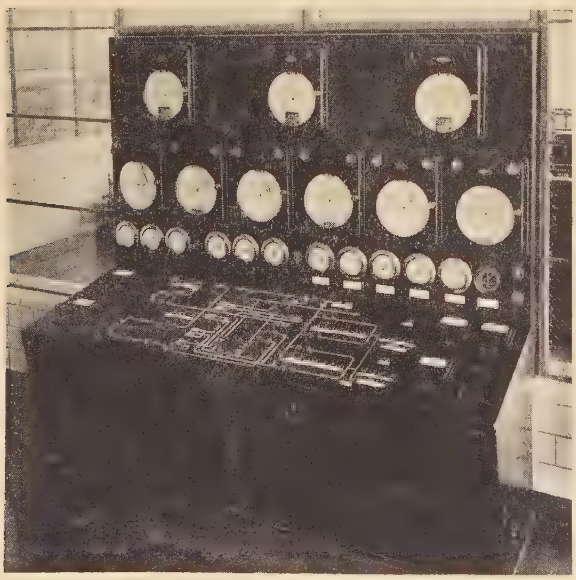

Figure 7. Control desk set into vaportight wire-glassed partition, through which the pump room is clearly visible

This breaker, normally controlled from the incoming line panel in the station, is located at the transformer substation approximately 300 feet from the pumping station. From the outdoor breaker, an underground circuit runs to a 2,300-volt bus in the switchgear room, which in turn feeds the main pump starters as well as a 2,300-to-460-volt transformer bank serving all the station auxiliaries. Energy for the control circuits of the motor starters and for signal lights and alarms is furnished at 230 volts through a transformer from the station auxiliary supply. A single-phase transformer connected to the 2,300-volt service ahead of the station power breaker supplies station lighting and outdoor lighting at 115/230 volts through separate low-voltage outdoor breakers. At some of the stations there are loads outside the station proper, such as fire pumps, water well pumps, and warehouse, which must be operable even when the pumping station is shut down. To carry these loads, as well as the lighting load, a 2,300-volt bus is provided on the outdoor structure, connected to the power service ahead of the station power breaker. To this outdoor 2,300-volt bus are connected the station bus breaker, the lighting transformer, a 2,300-to-460-volt transformer bank for outside auxiliaries and a 2,300-volt fire pump circuit.

With this arrangement of circuits, shown schematically in Figure 4, the station operator can clear all power circuits within the station proper by opening the 2,300-volt outdoor circuit breaker. This he can do either from within the station or at the substation. At the same time he can leave the station lights and outside auxiliaries in operation if required, since these circuits are not affected by tripping the outdoor breaker.

Control System

A fundamental consideration in establishing the control system was that of station attendance; it was decided at the start that:

1. In the interest of safeguarding property, each station must have attendance at all times.

2. During the tour of duty the attendant may be at any part of the station property, subject to prompt arrival at the control room upon signal.

3. In the simple booster stations the at-

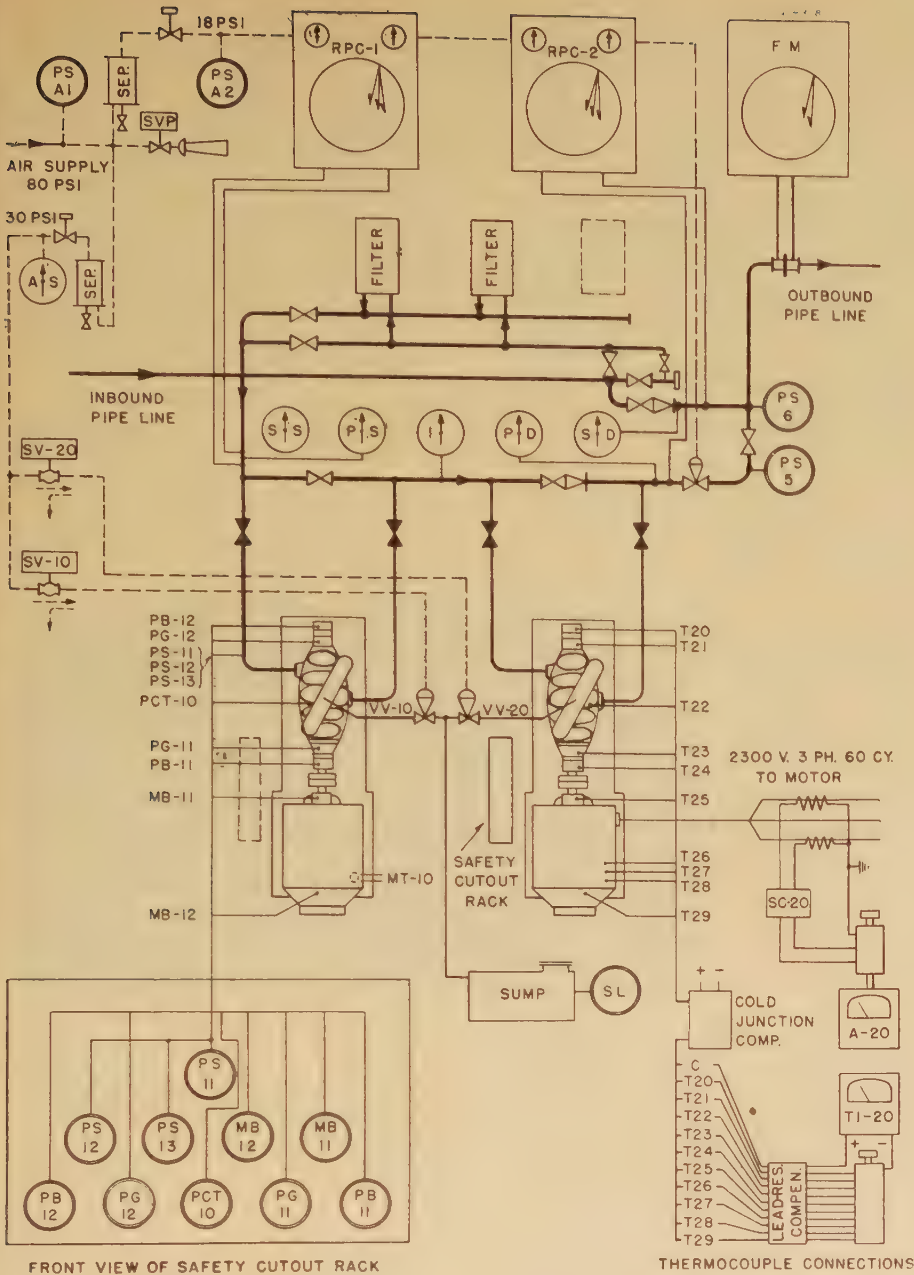

Figure 8. Schematic diagram of station control system for typical two-unit station

tendant will perform singly all operating duties incidental to starting, running, and stopping units, as required by the central dispatching office.

4. Maintenance work except of minor nature is to be handled by established maintenance personnel, progressing from station to station. In case of difficulty or failure in the operation of equipment, specialized repair personnel will be available on call to the superintendent of the division.

These operating conditions determine the following essential features of the system of control:

(*a*). Starting and normal stopping of the stations and of units within each station shall be controlled by the station attendant.

(*b*). Protective shutdown of units and stations must be instituted automatically when conditions arise that exceed predetermined limits of safe operation.

(*c*). In case of automatic protective shutdown, a signal must be given to inform the attendant that shutdown has occurred, and indication must be given of the specific protective function that caused shutdown.

(*d*). In the starting and stopping of main-line units, it is desirable to simplify the duties of the attendant by providing automatic operation in proper sequence for the valves, motor control, and pressure devices concerned. The operator, thus relieved from performing a routine of manual operations at different points in the station, can devote his attention to the over-all process and to the results obtained.

(*e*). For continuous operation of the line it is, of course, necessary to maintain pressure conditions within the limits predetermined for protective shutdown, and this should be accomplished by automatic means.

Design of Station Control

The control system developed to meet these requirements embodies the co-ordinated application of pressure, temperature, and electrical control and indicating devices.

Each pump unit normally is controlled from an individual "start–stop" push-button, the control system providing a predetermined sequence of operations. Considering a typical two-unit booster station, this control system provides automatic means for:

1. Venting the pump casings during shutdown to prevent building up of pressure within them by leakage past suction or discharge valves.
2. Permitting the starting of a unit only upon existence of a predetermined minimum suction pressure.
3. Operating in correct sequence a motor-driven suction valve and a motor-driven discharge valve for each unit.
4. Venting vapor from the pump casing and circulating liquid through the pump during starting.
5. Adjusting the station capacity to the line throughput while the station is on the line.
6. Limiting the minimum value of station suction pressure to a value safe for pump operation.
7. Limiting the maximum value of the station discharge pressure to a value safe from the standpoint of pipe-line stress.
8. Safety shutdown, sounding the station alarm and lighting an individual indicating light, in case of:

	See Figure 8 Symbol
Excessive duration of high motor starting current	*SC*-20
High motor-winding temperature (alarm and light only)	*MT*-10
Power-supply failure (alarm only—air operated)	*S V-P*
High motor-bearing temperature (front or rear bearing)	*MB*-11, *MB*-12
High pump-bearing temperature (front or rear bearing)	*PB*-11, *PB*-12
High pump-gland temperature (front or rear gland)	*PG*-11, *PG*-12
High pump-casing temperature	*PCT*-10
Low suction pressure pumps 1 and 2	*PS*-11, *PS*-21
High suction pressure pump 1 and 2	*PS*-12, *PS*-22
High station discharge pressure	*PS*-5
Low station discharge pressure	*PS*-6
Low air-supply pressure to hydraulic controllers (alarm and light only)	*PS-A*2
Low station air-supply pressure (alarm and light only)	*PS-A*1
High sump-tank level (alarm and light only)	*S L*

An ammeter (*A*-20) and ammeter switch are provided for each unit, also a common temperature indicator (*TI*-20) for both units with a selector switch for

each unit whereby the temperature indicator may be connected, per unit, to any one of:

	See Figure 8 Symbol
2 Pump-bearing thermocouples	*T*-21, *T*-25
2 Pump-gland thermocouples	*T*-22, *T*-24
1 Pump-casing thermocouple	*T*-23
2 Motor-bearing thermocouples	*T*-20, *T*-26
3 Motor-winding thermocouples	*T*-27,-28,-29

The gauge panel mounts a flowmeter (*FM*) for indicating and recording the rate of flow at or by the station, also two pressure controllers (*RPC*-1, *RPC*-2) whose function will be discussed below, and six gauges indicating pressure at the following points:

	See Figure 8 Symbol
Station suction	*SS*
Pump suction	*PS*
Interpump	*IP*
Pump discharge	*PD*
Station discharge	*SD*
Air supply to air operated vent valves	*AS*

Pump Venting

Each pump has a connection from the top of its casing through a vent valve to the station sump. The vent valve (*VV*-10, *VV*-20) is spring-opened, air-closed, the air supply being controlled by a three-way solenoid valve (*SV*-10, *SV*-20). This valve is so connected in the motor-control sequence that the vent valve is open while the pump unit is down and remains open during the starting cycle until the pump discharge valve is fully open, whereupon the solenoid valve is energized to close the vent valve. In a shutting-down cycle, when the pump discharge valve leaves its open position, the solenoid valve is de-energized to open the vent valve. At the conclusion of the shutting-down cycle both the discharge valve and the suction valve are closed, and flow through the pump-venting system ceases.

Station Pressure Control

The regulation of station pressure is accomplished by air-actuated instruments such that the station suction and discharge pressures can be automatically maintained at any predetermined values. The control valve which is actuated from these instruments is normally automatically controlled, but is equipped for manual operation in the event of air failure or interruption of electric service at the stations. All the stations operating in a continuous section of line are affected by any change in flow rate that may occur. There are many changes in flow rate throughout the entire system. These are occasioned by the existence of the large number of delivery points or terminals, which may be utilized for multiple deliveries or for single full-line capacity deliveries. The station pressure controls are so designed as to immediately adjust each station to these varying flow conditions and thereby maintain uninterrupted operation. The previously mentioned automatic shutdown features are co-ordinated with the regulating equipment, so as to permit operation of the control system over its full range, and only to prevent operation of one or two units in the station in case the pressures exceed pre-established limits.

Control Desk

Physically, the control for each station centers in a control desk in the control room, where also is situated the gauge board previously mentioned, the attendant's working desk, and a teletype operating over a circuit serving the system dispatcher's office and all pumping stations.

Red indicating lights on the control desk identify the functioning of each of the protective shutdown and alarm switches already mentioned. Amber and green indicating lights show respectively the open and closed positions of the motor-operated suction and discharge valves and of the principal manually operated valves in the station piping.

To clarify the function of each device the desk top bears a schematic layout of the principal piping for the particular station, and in this piping diagram the indicating lights, temperature indicator, ammeters, selector switches, and push-buttons are all mounted in appropriate position. An accompanying illustration shows how conveniently the control desk and gauge board in one of the stations serves as the central point for indication and control of all principal station functions.

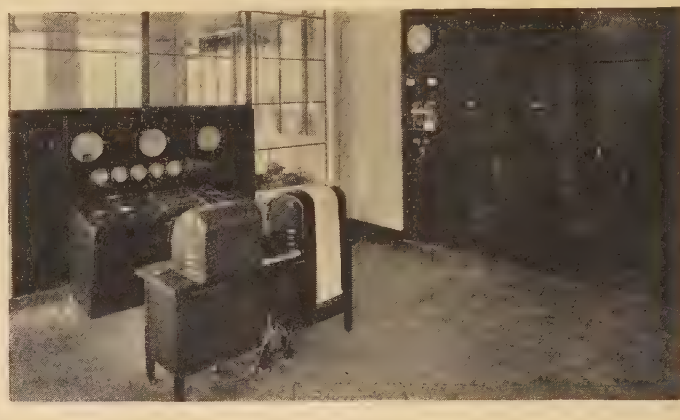

Figure 9. Control desk, gauge board, teletype and telephone facilities, and panels for incoming line, motor starters, and low-voltage power distribution, in a typical two-unit station

Conclusion

As evident from this discussion, the electrification of the Plantation system was designed to meet the practical requirements of products-line operation. That it adequately serves these needs is indicated by operating experience with the system to date, which has verified all fundamental features of its design.

Protection of Pilot-Wire Circuits

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THIS paper reviews the problem of protecting pilot-wire circuits, briefly discusses the sources of disturbing voltages, describes two types of neutralizing transformers, and illustrates how they are used to distribute the disturbing voltage most favorably throughout the pilot-wire circuit. The recent increase in use of high-speed pilot-wire relays for the protection of important power circuits has accentuated the need of better understanding of the pilot-wire protection problem. Therefore, a review of this general subject and a description of a new type of neutralizing transformer with mathematical formulas to determine its effectiveness, is considered timely.

Three principal objectives to be attained when applying pilot-wire protective apparatus are:

1. To assure correct operation of the protective relays or other apparatus at all times.
2. To protect operating personnel.
3. To protect the apparatus.

Pilot circuits for protective relaying must be operative at all times, especially at times of system disturbances. Thus, simple protective gaps which provide adequate protection for many other pilot services cannot be used to protect against voltages induced by the power-system currents, as this would render the circuit inoperative when most needed. Certain supervisory control and other functions are also required to be operative during power-system faults, and this discussion applies equally well to pilot circuits for these purposes. The principles and methods will also be found useful in applying protection to those telemetering and supervisory control circuits which are not required to be operative during power-system faults.

A pilot circuit may be exposed to disturbing voltages of several sorts, which will be discussed subsequently. Briefly, these are:

(*a*). Lightning and other surges.

(*b*). Induction from power lines.

(*c*). Elevation of station ground potential.

(*d*). Direct crosses with power circuits.

If these voltages are not to disrupt the proper operation of the circuit, they must be disposed of by some combination of the following measures.

(*a*). By insulating the circuit to stand them.

(*b*). By distributing them through design of the circuit impedances and grounds.

(*c*). By the use of gaps or arresters to prevent surge voltages from reaching excessive values.

(*d*). By drainage—dissipating the disturbing voltages as impedance drops in the circuits.

(*e*). By shielding the wires or so locating them that the disturbing voltages are lessened.

(*f*). By using insulating transformers or neutralizing transformers to take up longitudinal voltages as will be explained.

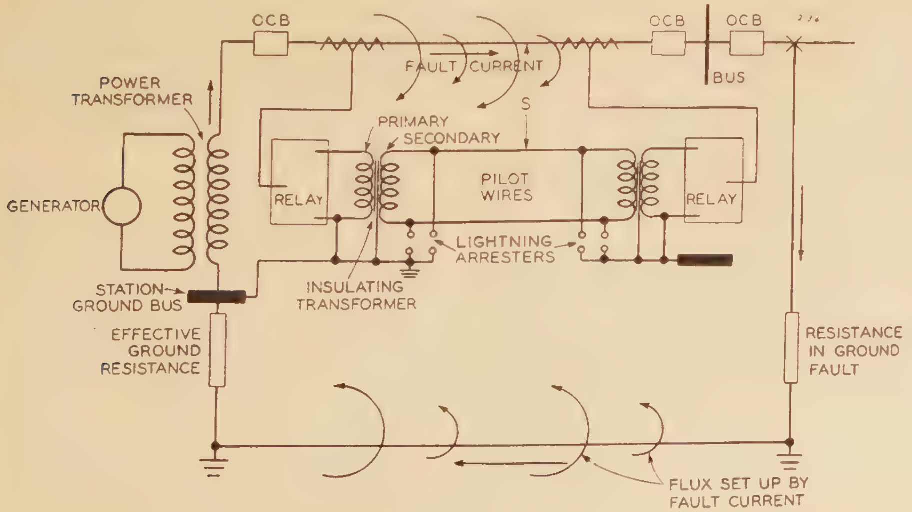
In many cases no specific protective measures are required. This is frequently the case when rubber-lead-covered control cable is used in relatively unexposed locations. However, a consideration of the magnitudes of disturbing voltages is worth-while in any case, to insure that the pilot circuit, through its insulation, arrangement, and protective features, provides adequately for the voltages to be encountered.

Pilot-wire circuits that are totally enclosed in a frequently grounded sheath are inherently provided with a high degree of protection against lightning. Circuits of this kind, that are not exposed to induction from transmission lines and that enter stations in which only small ground potentials can occur, require no special protective measures. The auxiliary equipment that will be discussed is needed only when calculation or measurements indicate voltages in excess of the normal insulation strength.

The general problem is very much as follows: Assume that lightning strikes the power line and causes a phase-to-ground fault at the far end of the line section that is protected with pilot-wire relays. Assume also that the pilot circuit is exposed to the effects of the same lightning stroke. Arresters on the pilot wires must operate to protect the relays and associated equipment. However, the arresters must immediately seal off and interrupt any power follow, so as to make the pilot circuit operable. The seal-off voltage of the arresters must be above the maximum voltage that can be impressed on them by induction in the pilot wires or by a combination of a rise in ground potential and induction which can be caused by the flow of transmission-line fault currents. If the arresters should fail to seal off, they would effectively short-circuit the pilot wires and block operation of the protective relays. Similarly, provision must be made to guard against the accidental operation of pilot-wire relays which may be caused by unsymmetrical operation of the arresters. This may be accomplished, for example, by connecting a low-voltage protector tube between the pilot wires, as shown in Figure 4 and by distributing fundamental frequency-disturbing voltages so

Figure 1. Schematic diagram of faulted transmission line and pilot-wire relay circuit

S = Separation between pilot wire and power line



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The authors acknowledge the assistance of J. E. Barkle in preparing many of the potential-gradient diagrams.

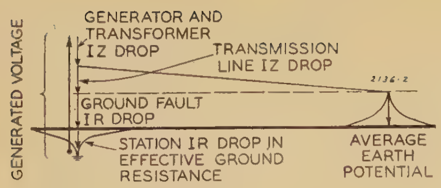


Figure 2. Distribution of voltages in transmission-line and ground circuits

IX_L component of line drop equals maximum possible voltage that can be induced in pilot wires

as to avoid operating the lightning arresters.

To aid in visualizing the manner in which these disturbing voltages may be impressed on the pilot-wire circuits by transmission line faults, consider the circuit that is illustrated in Figure 1. Inasmuch as these disturbances are most severe when one phase of the transmission line is grounded, only a single-phase system is drawn. As shown in Figure 1, assume that the faulted power system consists of a single-phase generator, a grounded transformer, a section of line, and equivalent ground-fault resistance. The pilot-wire circuit is spaced from the power-line conductor, a distance of S feet.

When the ground fault occurs, current will flow out through the transmission line and return through the ground. The generated voltage is used up in a series of voltage drops in the generator and transformer, line, ground-fault resistance, and the effective resistance that can be measured between the station ground bus and true ground. This resistance, indicated in Figures 1 and 2, varies from a fraction of an ohm in well-grounded stations to several ohms in stations that are not so well grounded. The value may be high or low, depending upon the earth resistivity in that vicinity. Ground potential rises of 1,000 to 3,000 volts are not uncommon, while values up to 15,000 volts are sometimes encountered.

The manner in which voltage is induced in the pilot-wire circuit is indicated in Figure 1. Fields of flux are set up by the flow of current in the transmission line and in the earth return. These fluxes link both pilot wires and induce voltages along the length of the exposed portion of the pilot-wire circuit in proportion to the magnitude of the flux linkages. If a twisted or spiraled pair of wires is used for the pilot circuit, no appreciable voltage difference will appear between the pilot wires. This is important, for the voltage difference that appears between the pilot wires must be limited to avoid operating the protective relay, or associated apparatus that is connected to the pilot circuit. Because of this, only longitudinally induced voltages, which are equal and in the same direction in both wires, will be discussed.

It will be noted that the flux which links the pilot wires from the ground current is 180 degrees out of phase with respect to that set up by the transmission-line current. Consequently, these two fields tend to cancel, or it may be said that the ground current tends to "shield" the pilot wires from induction. This effect becomes quite appreciable when ground wires carry a large portion of the ground-fault current.[1]

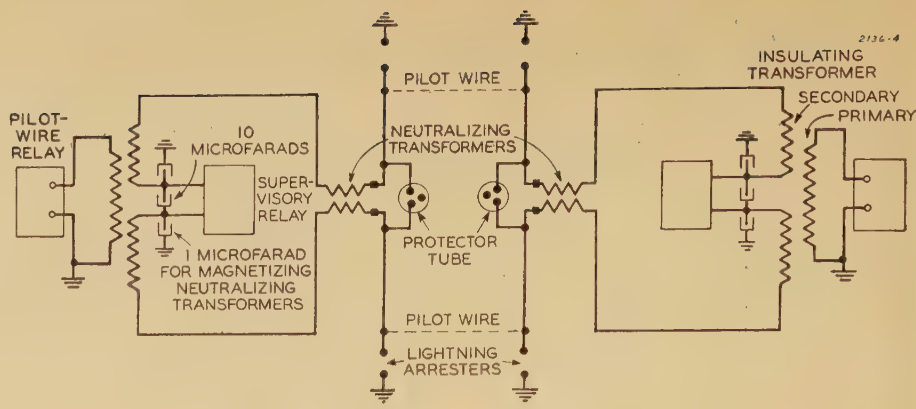
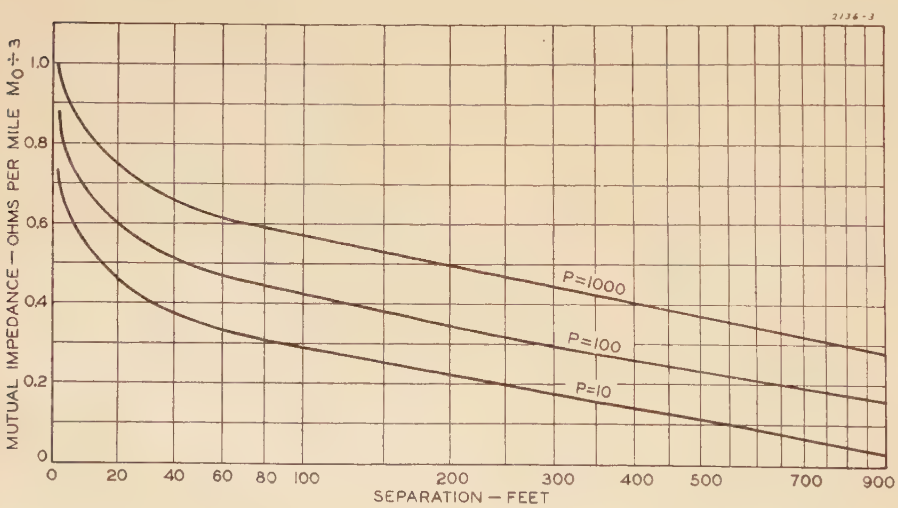


Figure 4. Protective apparatus

Figure 3 illustrates how the mutual impedance between the transmission line and pilot wires varies with the separation between the two circuits. Three different curves are drawn to illustrate how this quantity is affected by extreme variations in the resistance of the earth-return circuit or how the earth currents "shield" the pilot circuit from induction. When the earth resistivity is high, current returns deep in the ground and produces less shielding. The ground-fault current multiplied by the proper mutual impedance equals the magnitude of voltage that will be induced in the pilot-wire circuit. Note that this quantity can never exceed the magnitude of the transmission-line reactance drop. A voltage equal to that magnitude can be obtained only when the transmission line and pilot wires occupy the same space or have 100 per cent coupling. It should also be remembered that ground wires and return circuits that are provided by cable sheaths all tend to reduce the magnitude of the induced voltage. In general, the shielding that is obtained from a full-sized, lead-covered telephone cable will reduce the voltage that would be induced in an open-wire circuit by approximately 50 per cent.[1]

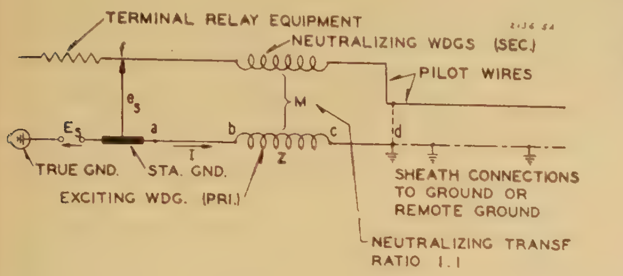
Figure 3. Approximate variation in mutual impedance between transmission circuit and pilot wires



Protective Measures

Disturbing voltages can be impressed on the pilot-wire circuits by lightning, electrostatic and electromagnetic induction, station ground potentials, and actual contact with power circuits. The damage which results from crosses with power circuits can be limited by solidly grounding and isolating portions of the circuit through action of arrester blocks and fuses. Frequently this will cause

relay operations that cannot be prevented. Voltages impressed on the circuit by lightning, induction, and ground potentials can be dissipated, distributed, or isolated with insulation in a manner that will not cause relay operation. This is accomplished by use of lightning arresters (which must be chosen so that they cannot be operated by system disturbances), by draining appreciable amounts of currents from the circuit through mid-tapped



transformers or similar balanced impedances, by altering the natural distribution of voltage stresses with neutralizing transformers or grounding impedances, and by insulating the apparatus or isolating it with insulating transformers so that it will withstand the applied voltage.

Insulating transformers have long been used to insulate the terminal equipment from disturbing voltages in pilot-wire circuits. They are also used to keep station ground potentials off the pilot wires. Insulation built directly into the relay can be used in a similar manner. This action is illustrated with typical graphical solutions in the latter part of this paper. The effects of electrostatic and electromagnetic induction can be counteracted directly in the pilot wires by current drainage, as by grounding the mid-points of the insulating transformers at both ends of the line. When this is done, it is necessary to consider thermal capacities of the equipment and inequalities in voltage drop in the individual pilot wires as any lack of symmetry in the circuit tends to insert an extraneous (unbalanced) voltage in the pilot loop and may affect the associated relay operation. Neutralizing transformers are used in circuits of this kind to reduce the circulating current to an absolute minimum and to provide a control means that can be used in d-c circuits.

To study the action of neutralizing transformers and determine the distribution of the disturbing voltages, pilot-wire equipment should be classified into groups at different potentials somewhat as follows:

1. Terminal equipment at station ground potential. In Figure 1 this includes the current transformers and relay parts up to and including the primary of the insulating transformer.

2. Terminal equipment on the station side of a neutralizing transformer. As neutralization is not perfect, this equipment operates at the remnant or unneutralized voltage to ground. In Figure 4 this includes the supervisory relay, secondary winding of the insulating transformer, and the capacitor between the two center taps of the insulating transformer.

3. The incoming pilot wires, any equipment connected thereto, and their potential with respect to ground.

Figure 5a. Three-winding neutralizing transformer neutralizing station ground potential

I = Neutralizing - transformer exciting current

r = Resistance of exciting-winding connections a-b and c-d

Z = Self-impedance of exciting winding

M = Mutual impedance between exciting winding and secondary windings

e_s = Remnant or unneutralized voltage which appears between relays and station ground

Z_p = Primary leakage impedance $= Z - M$

$$e_s = E_s \frac{Z_p + r}{Z + r} \quad (1)$$

Figure 5b. Three-winding neutralizing transformer neutralizing longitudinally induced voltage

Z = Impedance of ground return circuit, a to b

E = Longitudinal voltage induced equally in pilot and neutralizing wires

m = Mutual impedance between exciting wire-ground return circuits, from a to b

$Z_{sp} = Z_s - M_s$, $Z_{rp} = Z_r - M_r$, primary leakage impedances

Total remnant voltage:

$$e_s - e_r = E \frac{Z_{sp} + Z_{rp} + z - m}{Z_s + Z_r - z} \quad (2)$$

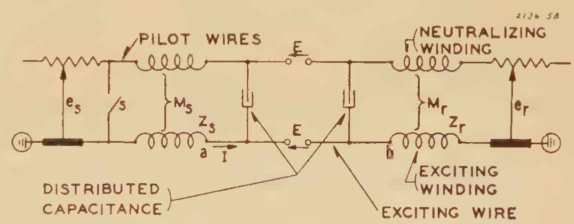
4. The cable sheath, usually at true ground potential if insulated, or at station ground if connected thereto.

5. Other wires in the cables, and their potential to ground.

Some of these items span from one potential to the other. The neutralizing transformers span from (3) to (2); that is, from the pilot wires to the remnant voltage point. The grounding capacitors span from (2) to (1); that is, from remnant voltage point to station ground. The insulating transformers span from (2) to (1).

Cable sheaths, if not connected to station ground, may usually be considered as at true ground throughout their lengths. They are usually grounded by leakage if not intentionally. Aerial cables with sheaths purposely insulated require a study of the various capacitive impedances, between wires and sheath, and sheath to ground, to determine the voltage with which their insulation is stressed. If a sheath is grounded along the line and also grounded to station ground, a heavy current will flow over it when there is a rise of station ground potential. The steepness of gradient along the cable sheath from station ground to true ground will depend on the resistance of the "pole grounds." Possible danger of burning the sheath should be checked, especially if the cable is buried under the power line so that it may carry a considerable part of the return fault current.

With given voltages, impressed on the pilot-wire circuit by longitudinal induction or a rise in station ground potential, the determination of voltage distribution as indicated above is a problem in conventional circuit theory. After determining each potential with respect to any convenient reference, such as true ground, the potential differences are obtained for verifying or designing the insulation co-



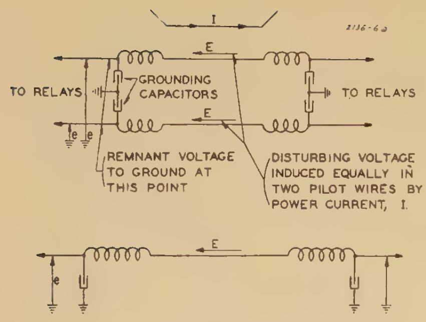
ordination. Graphic examples of this are given after operation of the neutralizing transformers is explained.

Three-Winding* Neutralizing Transformer

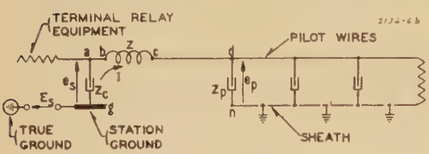
When used to protect against a difference in ground potential, the exciting winding of the three-winding neutralizing transformer is connected between true ground (remote pole ground) and the station ground as shown in Figure 5a. The pilot wires are connected in series with the secondary windings of the neutralizing transformer and thereby have a voltage almost equal and opposite to the station ground-potential rise impressed directly on them. Thus, with the pilot wires at true ground potential (sheath potential usually), the terminal relay equipment will be held close to station ground potential.

The connection from the exciting wind-

* Three winding is used to distinguish the type having a separate exciting winding. There may be any number of secondaries. Usually, there are two, through which a pair of pilot wires is carried.



(a) Actual and single-line representation of circuit with neutralizing transformers

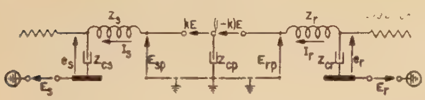


(b) Neutralizing station ground potential

r=Resistance of pilot wires in parallel from a to b and c to d

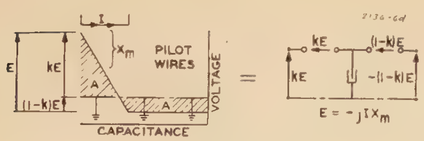
Z_p=Impedance of pilot wires to ground (usually sheath) looking into terminal d-n, including distributed capacity and terminating impedances

Z_c=Parallel impedance of grounding capacitors from a to g



(c) General case—neutralizing station ground potential and longitudinal induction

E=Total longitudinal induced voltage



(d) Equivalent circuit for pilot wires having induced voltage and distributed and lumped capacitance

Figure 6. Two-winding neutralizing transformer

ing to ground may be a wire carried out a few hundred feet to a pole ground. Or the cable sheath may be insulated for the last few hundred feet into the station and the exciting winding of the neutralizing transformer connected between it and station ground.

Assuming the pilot wires to be at true ground potential as indicated schematically by the dotted connection at d, Figure 5a, the remnant or unneutralized voltages between the terminal relay equipment and station ground can be expressed as a function of the station ground-potential rise, E_s, and the circuit constants shown. The voltage e_s, is the difference between the voltage drops from f to d, and a to d, due to I. Thus:

$$e_s = E_s\frac{Z_p+r}{Z+r} \qquad (1)$$

Commercial designs have been described[2] having 0.35 ampere exciting current with 4,000 volts applied to the primary winding. This results in an impedance Z = 4,000/0.35 or 11,400 ohms. The primary leakage impedance, Z_p, has been held to a value such that with a disturbing voltage, E_s, of 4,000 volts, the remnant voltage e_s, is 70 volts for $r=0$, and 81 volts for $r=35$ ohms. (r is the resistance of the exciting winding connections.)

The three-winding type of neutralizing transformer has also been used for neutralizing induced voltages.[3] In applications of this kind an exciting wire is run the full length of the pilot wires and has induced in it the same voltage as the pilot wires. However, the cost of the exciting pilot wire, which must be held to a low value of resistance, usually eliminates this scheme from consideration.

Two-Winding Neutralizing Transformer

The two-winding neutralizing transformer (or longitudinal choke) is a new form of neutralizing transformer in which the exciting current is conducted through the windings that are connected in series with the pilot wires and otherwise operates in the same manner as the three-winding neutralizing transformer. This design was introduced in 1935. The first application was for protection of extensive pilot wires along an a-c railway electrification.

The two-winding neutralizing transformer is designed to protect pilot wires and their terminal equipment from station ground potentials and longitudinally induced voltages. Its self-exciting feature is particularly valuable for applications which involve longitudinal induction, as it eliminates the need of a third pilot wire. Furthermore the device is more effective than the three-winding transformer, when used to counteract induced voltages, as the magnetizing currents flowing through the pilot circuit actually aid the neutralizing action.

By providing grounding capacitors as shown in Figure 6a, a complete excitation path is secured over the pilot wires. The resulting circuit which includes the induced voltage and the neutralizing transformers is termed a "self-exciting neutralizing-transformer circuit," since the exciting current is carried over the pilot wires which are also used for relaying.

The two windings of the neutralizing transformer are wound in the same direction on the core, so that currents which flow in the same direction in the pilot wires encounter exciting impedance, whereas relay loop currents, in opposite directions in the two pilot wires, encounter only the leakage impedance. The exciting impedance of such neutralizing transformers can readily be made as high as 100,000 ohms at 60 cycles with only 160 ohms added to the pilot-wire loop resistance. One-microfarad grounding capacitors, connected one from each wire to ground, have a parallel impedance of

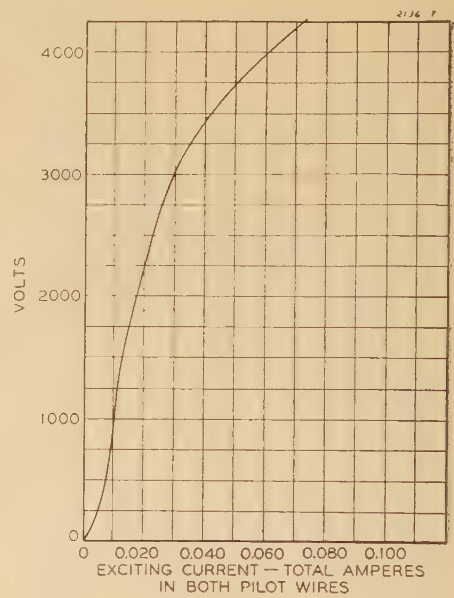


Figure 7. Saturation curve of typical two-winding neutralizing transformer (longitudinal choke coil)

1,325 ohms at 60 cycles and limit the remnant voltage to about 1.3 per cent of one half of the induced voltage. This assumes balanced conditions in which one half of the voltage is impressed across neutralizing transformers at each end of the pilot wires.

Station Ground Potential

The theory of the two-winding type of neutralizing transformer, as used for neutralization of station ground potentials, may be understood from a consideration of Figure 6. The two pilot wires are considered paralleled, so that the parallel impedances are used. The remnant voltage, e_s, of the terminal relay equipment from station ground is given by

$$e_s = -IZ_c = -E_s\frac{Z_c}{Z_c+Z+Z_p} \qquad (2)$$

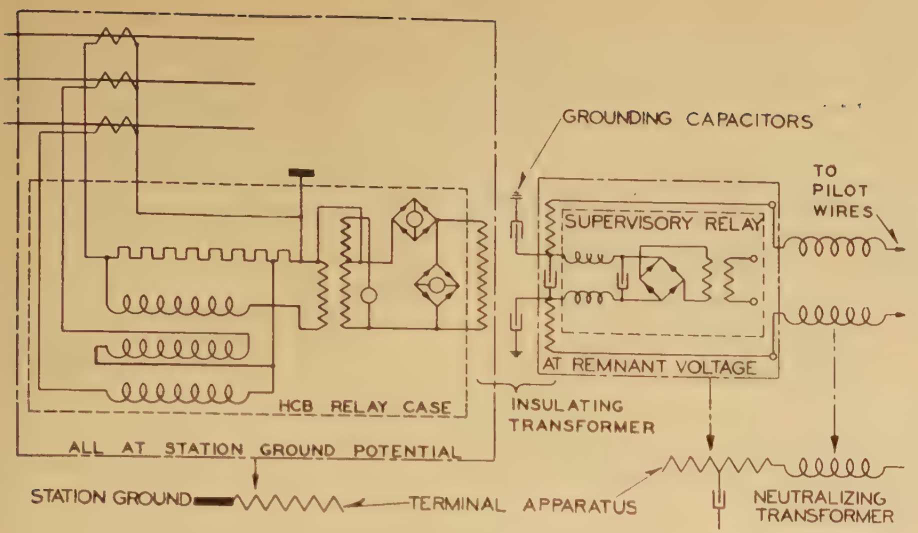


Figure 8. Actual connections

Arrows show parts included in each symbol of the potential-gradient diagrams

The voltage from the pilot wires to the cable sheath is

$$e_p = -IZ_p = -E_s \frac{Z_p}{Z_c + Z + Z_p} \qquad (3)$$

In practice, the neutralizing-transformer exciting impedance Z is very large, compared with the other impedances in the circuit. Therefore, the neutralizing transformer has impressed on it approximately the full disturbing voltage, E_s. The corresponding exciting current can be determined by reference to its saturation curve, see Figure 7. The remnant voltage, e_s, is then numerically the product of the exciting current, I, and the impedance, Z_c of the grounding capacitors (see equation 2). Similarly, the voltage from pilot wires to sheath is the exciting current I multiplied by Z_p, defined in Figure 6b (see equation 3). If the distributed capacitance is small, the voltage drop from the pilot wires to the sheath may be high. This can be reduced by adding lumped capacitance ground at d or terminating impedance to ground at the other end of the line.

Figure 7 shows the excitation characteristics of a commercial design of a two-winding neutralizing transformer. At 3,000 volts the exciting current is 30 mils (15 mils in each winding), or the exciting impedance, Z is 100,000 ohms. At 4,000 volts the exciting current is 0.062 ampere. The impedance inserted in the relay loop circuit by this design is $160+j\,130$ ohms, per transformer, at 60-cycle frequency.

Longitudinal Induction

The theory of the two-winding neutralizing transformer, used for protection against longitudinally induced voltages, is given in the appendix. This treatment covers the general case in which both longitudinal induction and station ground-potentials are present. Remnant voltages for the simpler case of longitudinal induction only are obtained using the same equations with the station ground potentials set equal to zero.

The potential distribution is influenced by the relative values of terminating impedances to ground and distributed impedances to ground as well as the locations and amounts of induced voltages. These factors are taken into account in the exact theoretical treatment of the appendix. The important case in which a station ground-potential rise and longitudinal induction are both caused by the same primary fault current is fully developed, the voltage distribution being expressed by equations 5a to 9a of the appendix.

Potential Distribution for Typical Conditions With and Without Neutralizing Transformers

A reasonably accurate conception of the potentials existing in various parts of a pilot-wire circuit that is exposed to station ground potential or longitudinal induced voltage may be obtained from the typical diagrams in Figure 9. The potentials shown are based on 2,000 volts station ground potential or 4,000 volts longitudinal induction as indicated, but can be ratioed for other values of disturbing voltage. Various parts of the circuit are represented by symbols drawn in position to show the voltages to ground at which those parts operate under the abnormal conditions. In this discussion, operating voltages between wires are taken as zero, and the two wires are represented as one for determining voltages to ground. The operating voltages are usually negligible in comparison but can be superposed if appreciable.

Figure 8 illustrates which parts of a typical pilot-wire relay are included in terminal apparatus at "station ground potential," and which part operates at the "remnant voltage" to ground. The insulating transformer presents negligible impedance to pilot-wire currents in the same direction (two-winding neutralizing-transformer exciting currents) which flow to ground. It may, therefore, be considered as operating at the remnant voltage, although physically it is in the circuit between the terminating capacitances and the neutralizing transformer.

Parts (a) to (d) of Figure 9 show the distribution of 2,000 volts station ground potentials. Part (a) is without neutralizing transformers; it illustrates the distribution of voltage that is obtained when an insulating transformer is connected between the relay and pilot wires. Note that the relay is held at station ground potential, and the entire voltage difference is impressed across the transformer insulation. If a supervisory relay were connected directly to the pilot wires, with its base connected to station ground, its insulation would be stressed with the full 2,000 volts rise in ground potential.

If the cable sheath is tied to station ground, the rise in potential of the pilot wires depends on the relative lengths of pilot wires affected by sheath sections at true ground and higher potentials. For example, if one tenth of the length of cable sheath is elevated 2,000 volts and nine tenths is at true ground, the wires will rise to ten per cent of 2,000 volts, or 200 volts above true ground. If only one per cent of the sheath is elevated, the potential rise will be a corresponding proportion of the rise in the short length of sheath. A wire-to-sheath potential approaching the full station ground potential must then be provided for in the cable insulation. This illustrates that voltage stress on the cable can be reduced by insulating the cable sheath from station ground.

The use of two-winding neutralizing transformers is illustrated in parts (b) and (c). This transformer holds the relay parts that are metallically connected to the pilot wires at station ground potential, except for the small remnant voltage, caused by the neutralizing transformer exciting current passing through the capacitance to ground. These remnant voltages are usually under 100 volts. The effects of predominant and negligible pilot-wire distributed capacity are also shown in (b) and (c). The rise in pilot-wire potential indicated in (c), can be reduced by connecting small fixed capaci-

tors between the pilot wires and ground. The circuit then becomes equivalent to that shown in (b). In each case, the effect of connecting the sheath to station ground is shown. The resulting condition is tolerable, if the cable has ample insulation, and its sheath has ample current-carrying capacity. However, the sheath must be insulated from station ground if the cable is not insulated to stand 50 per cent to 100 per cent of the ground potential rise, with a suitable margin.

As shown in part (d), the three-winding neutralizing transformer produces a potential distribution under conditions of station ground voltage rise, substantially the same as for the two-winding neutralizing transformer with predominant distributed capacity.

The distribution of potentials resulting from 4,000 volts of longitudinal induction is illustrated in parts (e), (f), (g), and (h), taking into account the following variables:

With and without neutralizing transformers (two-winding type).

Pilot-wire capacitance predominant or negligible.

Different exposure conditions.

Parts (e) and (f) show that with predominant distributed capacity, introduction of neutralizing transformers leaves the pilot-wire voltages unchanged and simply lowers the potential of any relay equipment metallically joined to the pilot wires, to substantially station ground potential.

The natural distribution of pilot-wire potential determined by distributed capacitance, as explained in the appendix, can be modified by terminating capacitances to ground indicated in Figure 9(c) and (g). The distributions are shown, in part (g), for the case of distributed capacitance negligible compared with these terminating capacitors, and in part (c) for the case of distributed capacitance predominant. Other cases fall in between. The unbalanced voltage distribution which results from heavy induction near one end, when distributed capacitance is predominant, part (c), can be equalized by the addition of terminating capacitances.

As shown in parts (f) and (h), the neutralizing transformers operate to hold the relay equipment substantially at station ground potential except for the remnant voltage. Part (h) of Figure 9 also illustrates the use of a number of neutralizing transformers, connected at intervals in the pilot-wire circuit. This expedient is used to reduce voltages on the cable itself, as neutralizing transformers at the

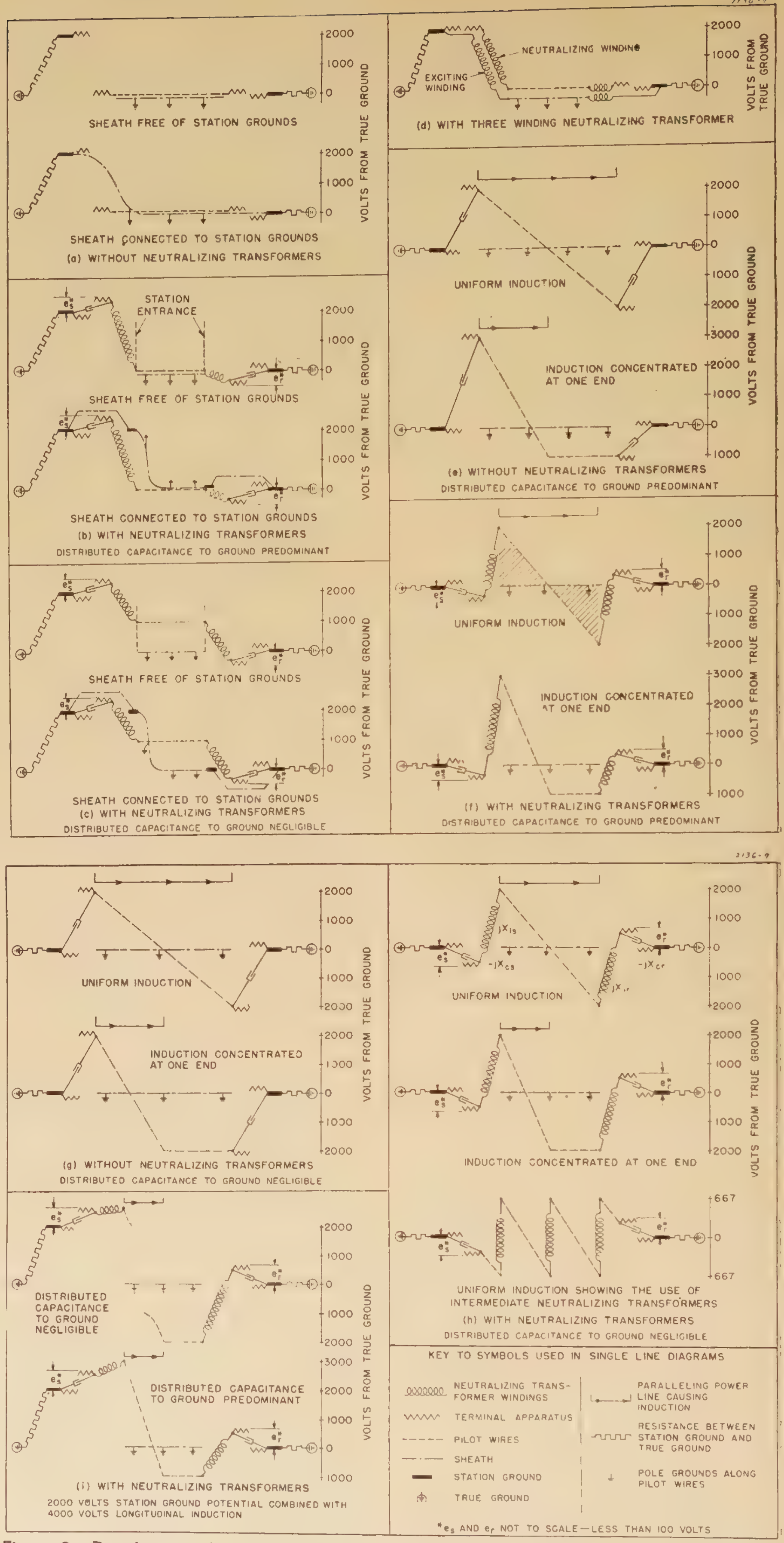


Figure 9. Distribution of wire-to-ground voltages caused by station ground potential and longitudinal induction

(a) to (d)—2,000 volts station ground potential

(e) to (h)—4,000 volts longitudinal induction

(i)—Combination of 2,000 volts station ground potential and 4,000 volts longitudinal induction

terminals provide adequate protection for the relay equipment.

Part (i) illustrates the potential gradient that is obtained when a combination of station ground potential and longitudinal induction exists. As one of these voltages is produced by resistance coupling and the other by mutual reactance coupling from the same current, the two are 90 degrees out of phase with each other. (The voltage produced by mutual coupling is ahead, if the reference directions are used as indicated in Figure 6, and the pilot wires are more closely coupled to a power phase wire than to the ground return.) As a result the voltage along the pilot wires varies not only in magnitude but also in phase position. Figure 9(i), shows the voltage magnitude to ground, of the principal parts of the circuit, but does not indicate the magnitude of voltage across the neutralizing transformers. The vectorial shift of the voltages along the pilot wires is illustrated in Figure 10.

The distribution of voltages that are imposed on pilot-wire circuits and their terminal equipments, by induction and station ground potentials, can be determined by use of the equations and diagrams presented herein. It will be noted that in many instances this can readily be accomplished by first determining the distribution of induced voltages, per Figure 6d, and then adding the rise in ground potential at right angles to it at the correct location in the circuit. The maximum stress on the pilot-wire insulation can be determined in this manner. If relay equipment is connected directly to the pilot-wire circuit, its insulation (and operating characteristics) must be able to withstand such a voltage, or neutralizing transformers must be installed to dissipate it and reduce the voltage stress at the relay to meet operating limitations. After the voltage distribution has been determined, as mentioned above, the effect of neutralizing transformers, or the use of grounding impedances, or of insulating transformers can be determined by simply subtracting the impedance drops from the voltages that are impressed on the circuit.

In general, all parallel wires that enter a cable at a given point should be protected in a similar manner to make certain that all wires within the cable are held at the same potential. A circuit of this kind should be studied, and the necessary protective measures must be taken to avoid the transfer of disturbing voltages from one set of wires to the other.

The paper has reviewed the problem of protecting pilot-wire circuits with emphasis on high-speed relay requirements and has presented mathematical and graphic aids to help the reader visualize it. It has also presented a mathematical analysis of a new tool, the two-winding neutralizing transformer, that is well suited to use in dissipating disturbing voltages that are frequently impressed on pilot-wire circuits. Use of this "tool" and the methods of analysis described should be of value to the users of pilot-wire circuits in avoiding difficulties that have been previously overlooked. The important points to be checked are the adequacy of insulation provided at different points in the circuit, and the voltage that is impressed on the lightning arresters. This latter point is of primary importance on leased circuits that are used with high-speed protective relays. The voltage distribution must be such that lightning protection is never operated by power-system disturbances.

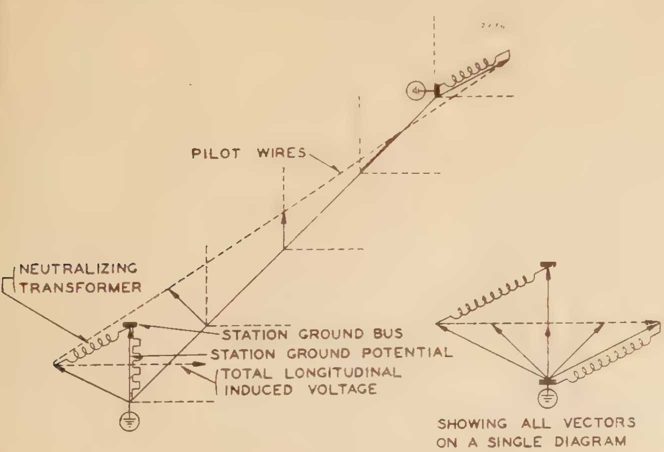
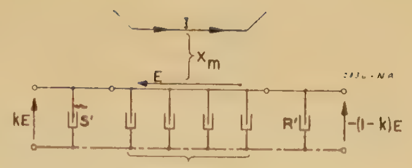


Figure 10. Vector voltage distribution along pilot wire exposed to both station ground potential and longitudinal induction

Appendix. General Case of Longitudinal Induction and Ground Potential

In the general case where both station ground potentials and longitudinal induced voltages are present, the voltage distribution can be determined in the following manner: First determine the distribution of the induced voltage in the pilot wires to obtain the value k; this is done with the terminal

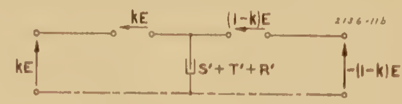


(a) Actual circuit

Uniform section of distributed capacitance and induced voltage

Total shunt capacitance in this section $= T^1$

Total induced voltage $= E$



(b) Circuit equivalent to (a) as far as terminal conditions are concerned

Figure 11. Potential gradient for induction in part of a section

$$k = \frac{2R'+T'}{2(R'+T'+S')}$$

Z_{cp} = Impedance corresponding to $S'+T'+R'$

All capacitances in microfarads

equipment disconnected. Then complete the circuit to calculate the distribution of voltages in the entire circuit as will be explained.

As shown in Figure 6d, the pilot-wire circuit can usually be replaced by a T section composed of two parts of the induced voltage, kE and $(I-k)E$, and the total shunt capacitive impedance of the two wires to ground, Z_{cp}. This assumes that voltage drops in the series impedance of the pilot wires, caused by the shunt capacitive current, are negligible. The value of k can be determined by considering the voltage distribution in the pilot wires with the neutralizing transformers and terminal equipment disconnected. If shunt impedances are all capacitive, either distributed or lumped, the voltage distribution diagram can be constructed as shown. The positive and negative areas A are made equal, since they are proportional to the charging currents entering and leaving the pilot wires, which must be equal. This is most easily visualized by referring to Figure 6d. Note that the abscissa is capacitance, rather than distance to allow for cable or open wire, and lumped capacitance effects. Thus the potential distribution diagram can be readily drawn from the known capacitances and induced voltages by making the areas equal.

One of the most common cases is shown in Figure 11. Uniform induction, of total voltage E, occurs over a section having uniformly distributed capacitance, totaling T' microfarads. The sum of remaining distributed capacitance plus lumped capacitance at one end is S' and at the other end, R'. For this case

$$k = \frac{2R'+T'}{2(R'+T'+S')} \tag{4}$$

After the potential distribution in the pilot wire above has been determined, that is, the value of k fixed, the equivalent diagram can be connected to the terminal equipment as in Figure 6c. A vector solution of the resulting two-mesh network gives the various voltages and exciting currents.

120-Kv Compression-Type Cable

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THE oil-filled type of high-voltage paper-insulated cable has been used on all extra high-voltage underground installations now in service in this country. While the major portion of these installations operates at oil pressures under 30 pounds per square inch, several of the shorter ones utilize oil pressures up to 200 pounds per square inch ("oilostatic" type). Although an alternate system[1] in which a high gas pressure is applied externally to the lead sheath has been in commercial service abroad since 1932, it was not until recently that this type of cable, known as compression cable, received serious study here. In 1939 the Detroit Edison Company found that a system employing a welded steel pipe line offered a number of advantages over the conventional duct installation under the conditions existing over the route of a projected 120-kv underground circuit. For this reason the companies with which the authors are associated initiated an intensive laboratory and field investigation of the technical characteristics of several systems employing a steel pipe. The present paper describes some of the more interesting results of the technical investigation on the compression cable system.

Essentially the compression-cable system comprises a cable that is in most respects an ordinary solid-type cable encased in a pressure chamber in which a gas pressure of approximately 200 pounds per square inch is applied externally to the lead sheath. In any high stress design it is necessary to eliminate entirely or nullify effectively the two important inherent defects of ordinary solid-type cable:

1. The expansion and contraction of the impregnating compound with the load cycle must be provided for.

2. The small but inevitable gas occlusions within the insulation must be rendered innocuous.

In the low-pressure oil-filled cable the first requirement is taken care of by incorporating an oil-flow channel or channels within the cable and by locating along the route reservoirs having expansible cells and an external gas supply to maintain positive pressure. The second prerequisite is met by employing a design which allows more complete degassing of the oil and saturation of the paper than obtained in ordinary solid-type cable. Several investigators have patented designs in which impermeable expansible members and the associated gas supply are incorporated in the conductor or sheath of the cable itself. Because of economic considerations none of these latter designs has attained commercial recognition. In the compression cable system the cable is made noncircular so that the lead sheath by change of shape may serve as an impermeable expansible member, the action of which is made reversible by external gas pressure. Thus the cable becomes self-compensating in that on increasing load the volumetric increase is accommodated by an increase in the cross-sectional area, while on decreasing load the external gas pressure causes the reverse action to take place, thereby preventing void formation as a result of load changes.

In actual practice the compression cable is made oval in cross section so that the shape is free to change in such a direction as to alter the minor axis at the expense of the major axis and hence alter the cross-sectional area without necessitating any large change in the periphery of the cable. A thin metal binder tape with paper tape over and under it is applied over the lead sheath in order to insure uniform diaphragm action along the sheath. The external gas pressure is secured by placing the cable in a welded steel pipe line filled with nitrogen gas under pressure. By actual field and laboratory experience it has been found that a gas pressure of about 200 pounds per square inch is adequate to maintain a positive internal oil pressure sufficiently high to prevent ionization in those voids

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These are

$$I_s = \frac{-E_r Z_{cp} + Z_{cp}(1-k)E + (Z_{cr}+Z_r+Z_{cp})(kE+E_s)}{(Z_{cs}+Z_s+Z_{cp})(Z_{cr}+Z_{cp}+Z_r)-Z_{cp}^2} \qquad (5)$$

$$e_s = Z_{cs} I_s \qquad (6)$$

$$e_r = e_s - E - E_s + E_r \qquad (7)$$

$$E_{sp} = -E_s + I_s(Z_s+Z_{cs}) \qquad (8)$$

$$E_{rp} = E_{sp} - E \qquad (9)$$

E_r and E_s = station ground potentials at r and s

E = voltage induced in the pilot wires

Frequently, the shunt capacitance to ground of the pilot wire will be so large that the potential distribution of the pilot wires will be relatively unaffected by the connection of the neutralizing transformers and terminating capacitors, Z_{cs} and Z_{cr}. Test of this condition is whether the neutralizing transformer impedance (approximately 100,000 ohms for a commercial design) is large compared with the capacitive reactance to ground (sheath) of the pilot wires. In this case corresponding to $Z_{cp}=0$, the disturbing voltages can be taken as E_s+kE at the "s" end and $(1-k)E$ at the "r" end. The voltages e_s and e_r can then be determined by multiplying Z_{cs} and Z_{cr} respectively by the neutralizing transformer exciting current (Figure 7) corresponding to these disturbing voltages.

A practical case of considerable importance is that of station ground potential at only one end, combined with longitudinal induction from the same power current. For this case, represented in Figure 6c by $E_r=0$, the longitudinally induced voltage E is 90 degrees ahead of the station ground potential E_s. Thus taking E_s as reference, $(\bar{E}_s = \bar{E}_s)$ equations 5 to 9 reduce to 5a, to 9a, as shown below:

When $E_r=0$ E_s = reference = $\bar{E}_s$ $E=90°$ ahead $= j\bar{E}$

Substituting these quantities in equations 5 to 9.

$$I_s = \frac{jZ_{cp}(1-k)\bar{E} + (Z_{cr}+Z_r+Z_{cp})(jk\bar{E}+\bar{E}_s)}{(Z_{cp}+Z_s+Z_{cp})(Z_{cr}+Z_{cp}+Z_r)-Z_{cp}^2} \qquad (5a)$$

$$e_s = Z_{cs} I_s \qquad (6a)$$

$$e_r = e_s - j\bar{E} - \bar{E}_s \qquad (7a)$$

$$E_{sp} = \bar{E}_s + I_s(Z_s+Z_{cs}) \qquad (8a)$$

$$E_{rp} = E_{sp} - j\bar{E} \qquad (9a)$$

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which are inherent in solid-type cable when operated in the usual manner. In consequence of the application of this principle of using high gas pressure in conjunction with an oval-shaped cable and the lead-sheath separator, it is possible to operate ordinary solid-type insulation at voltage stresses and conductor temperatures comparable with those used on the oil-filled type of cable.

Experience Abroad

The first commercial circuit of compression cable was installed in England in 1932. The cable used in this initial 66-kv installation was a three-conductor type *HSO* design, that is, a conventional three-conductor type *H* cable with round conductors and binder tape and a triangular-shaped sheath, the latter being obtained by omitting the major portion of the filler material. In order to distribute the diaphragm action as uniformly as possible over the flat sides of the triangular-shaped sheath, a metal binder tape with an asphalted paper tape over and under it, was applied over the sheath. A rectangular steel wire armor applied with a long lay completed the makeup. The armor wires were considered necessary in order to permit the pulling in of a long length of cable without injury. It is the practice abroad to allow these armor wires to take the entire strain of the pull.

While from an electrical point of view this installation has operated satisfactorily since being placed in service, yet on the basis of subsequent experience and laboratory tests the use of the type *HSO* construction has been discontinued abroad and another construction known as type *HSL* has been adopted, because it is easier to manufacture and lends itself better to diaphragm action of the lead sheath. In the latter design three oval-shaped conductors, each separately insulated, leaded and reinforced with several metal tapes, are cabled together with a short lay and armored with steel tape.

In addition to the two designs mentioned, both of which are installed in a steel pipe, a third type known as the self-contained compression cable has been developed abroad. In this design the function of the steel pipe is accomplished by an outer lead sheath which is separated from the diaphragm sheath by a spacer wire to provide a gas channel and is suitably reinforced to withstand high gas pressure. It was at first considered that this design might find some application for duct systems in this country. However, to protect the metallic reinforcing tapes against mechanical damage and corrosion, a third lead sheath would be desirable. With the large conductor sizes commonly employed here, the resulting sheath losses, which cannot easily be prevented, would be excessive. In view of these facts, this type cannot be justified economically.

At the end of 1940 there were over 55 conductor miles of compression cable in service abroad operating at voltages ranging from 50 to 120 kv. Operating experience on these installations has been entirely satisfactory from an electrical standpoint. Electrolytic corrosion of the pipe was experienced on the first installation in England as the result of poor pipe coating, and mechanical trouble near the joint wipes was encountered on a 50-kv installation in Copenhagen because of inadequate provisions for the differential longitudinal movement between cable and pipe.

Because of the difficulty of obtaining precise information on all phases of the foreign installations, and the hazard involved in extrapolating foreign experience to relatively large loads and consequently large conductor sizes normally used in this country, it seemed advisable to recheck fully the mechanical features of the system in the laboratory and in the field by means of an experimental installation.

Preliminary Studies

Diaphragm Action

It was first essential to determine whether the lead sheath could withstand indefinitely the diaphragm action resulting from temperature changes occurring during normal operation of cable. Accordingly extensive laboratory tests were made on pure lead sheath simulating the normal conditions of operation.

It is reasonable to assume that the maximum daily temperature change would be approximately 20 degrees centigrade, which would correspond to a volume change of one-half of one per cent. This about equals the volume difference between an oval-shaped sheath in which the ratio of the minor to major axis is 0.9 and a circular sheath of the same perimeter. Assuming the maximum seasonal change to be 80 degrees centigrade, a volume change of two per cent would occur. To obtain the desired information on the mechanical performance of the lead sheath it was necessary to subject the samples to changes in volume corresponding to both these daily and annual changes. The two per cent volume change would occur once for every 365 of the one-half of one per cent volume change cycles. The test program adopted for most of the measurements consisted in subjecting the samples to one two per cent volume change and 300 one-half of one per cent volume changes in a 24-hour period. This accelerated laboratory test program subjected samples in a single day to the equivalent of approximately one year's diaphram action under normal operation.

Samples of compression cable 32 inches long were used for the tests. The diaphragm action or change of volume was

Table I. Tests of Diaphragm Action of Lead Sheath

Sample Number	External Pressure Medium	Internal Pressure Medium	Type of Cycle	Temp.	Lead Thick. Mils	Origin of Cracks	No. of Cycles to Failure
M-8	*C*	*A*	½–2% 4.8 min.	Room	90	Outside	4,900
O-9	*C*	*A*	½–2% 4.8 min.	Room	90	Outside	5,800
O-10	*C*	*A*	½–2% 4.8 min.	Room	90	Outside	4,500
M-9	*C*	*A*	½–2% 4.8 min.	Room	90	Outside	5,000
M-10	*C*	*A*	½–2% 4.8 min.	Room	90	Outside	4,100
N-*a*	*C*	*A*	½–2% 4.8 min.	Room	90	Outside	4,460
P-1*a*	*C*	*A*	½–2% 4.8 min.	Room	90	Outside	4,160
O-11	*C*	*A*	½–2% 4.8 min.	Room	90	Outside	4,460
O-12	*C*	*A*	½–2% 4.8 min.	60 C	90	Outside	4,100
M-11	*C*	*A*	½–2% 4.8 min.	60 C	90	Outside	3,100
N-1	*C*	*A*	½–2% 4.8 min.	60 C	90	Outside	3,450
P-2*a*	*C*	*A*	½–2% 4.8 min.	60 C	90	Outside	3,500
P-4	*C*	*A*	½–2% 20 min.	Room	90	Outside	3,100
Q-1	*C*	*A*	½–2% 4.8 min.	Room	80	Outside	3,000
Q-2	*C*	*A*	½–2% 4.8 min.	Room	80	Outside	3,200
Q-3	*C*	*A*	½–2% 4.8 min.	Room	80	Outside	3,250
O-13	*B*	*A*	½–2% 4.8 min.	Room	90	Inside	11,500
O-14	*B*	*A*	½–2% 4.8 min.	Room	90	Inside	11,800
O-15	*B*	*A*	½–2% 4.8 min.	Room	90	Inside	13,000
O-18	*B*	*B*	½–2% 4.8 min.	Room	90	Both sides	24,000
O-19	*B*	*B*	½–2% 4.0 min.	Room	90	Both sides	15,240
P-4*a*	*D*	*B*	½–2% 4.0 min.	Room	90	Both sides	17,000

A—Oil initially degassed, but becoming slowly contaminated with air.

B—Degassed oil protected by Sylphon diaphragms from air contamination.

C—Compressed air.

D—Nitrogen (water-pumped).

produced by causing pressure differentials between the inside and outside of the cable sheath. At the start of the tests oil, not highly degassed, was used as a pressure medium on the inside of the sheath and compressed air was used directly on the outside. The first eight samples tested on the cycle described gave a life ranging from 4,100 to 5,800 cycles (number of one-half of one per cent volume cycles) at room temperature. Examination of these samples after failure showed evidence that the incipient cracks which occurred had originated on the outside surface of the sheath. Since that surface was in direct contact with the compressed air it seemed likely that oxidation may have influenced the results. Subsequent tests were made in which carefully degassed oil was used as an external pressure medium. This change immediately resulted in a life ranging from 11,500 to 13,000 cycles. Further precautions to insure carefully degassed oil on the inside as well as the outside of the sheath resulted in a life ranging from 15,240 to 24,000 cycles, that is, a life approximately four times as great as that obtained when no precautions against oxidation of the sheath had been taken.

Table I gives the results of laboratory tests on five different kinds of samples (*M*, *N*, *O*, *P*, *Q*) each representing some change in either sheath thickness, tension of paper tapes during manufacture, or in degree of looseness of armor tapes. In general these structural differences had no significant effect on the life of the sheath.

Samples tested with air as a pressure medium show that thinner sheaths and higher temperatures decrease the ability to withstand diaphragm action. This may be attributed to the effect of oxidation.

The test conditions differed from service conditions in two important respects, namely:

(*a*). That each cycle is carried out in about 1/300 the time that would occur in practice.

(*b*). That the volume ranges in the test cycles correspond to load conditions which are possible in service but are not normal for most installations.

In both of these respects, the test conditions are the more severe. The stresses required to produce the same amount of deformation in the sheath are very much less for slowly applied deformation. For a given deformation where the lead is not exposed to oxidation as under service conditions, a greater number of cycles is required to produce failure when the period is long than when it is short. In view of this and the fact that the number of full volume daily cycles per year will generally be less than 300, the approximate 50 years represented by 15,000 cycles is a conservative estimate for the life of the lead sheath.

Longitudinal Movement

The usual practice for armored compression cable is to anchor the joints; however, it was felt that if the armor were eliminated, and the three conductors cabled with a comparatively short lay, the longitudinal movement would be restrained without undue stress on the cable or joints. The following is a brief account of the tests designed to measure the magnitude of such restraining forces.

The three conductors of a 50-foot section of 600,000-circular-mil unarmored compression cable were cabled to a six-foot lay, but not bound together, and then pulled into a six-inch inside diameter iron pipe of the same length. At one end the cable was anchored to the pipe, at the other end means were provided for applying and measuring a restraining force of such magnitude as to prevent any movement of the cable with respect to the pipe end. The cable was heated by circulating current in the conductors. Thermal equilibrium was reached with conductor, sheath, pipe, and ambient air temperatures of 83, 67, 43 and 23 degrees centigrade, respectively. The maximum restraining force required to prevent longitudinal movement during the thermal transition period was 475 pounds, which dropped to 350 pounds when the steady-state temperatures were reached.

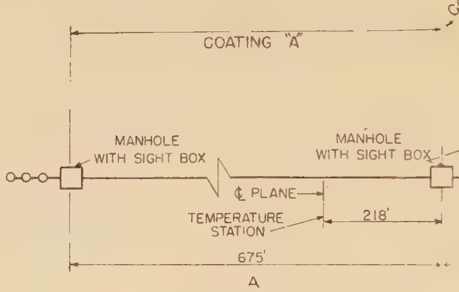
In view of the very small forces developed by the cabled conductors, a similar test was made on the same type of cable, in which the conductors were not cabled or bound together in any manner. The final restraining force was 3,000 pounds, with a maximum of 3,500 pounds reached during the transition period, for a final conductor temperature rise of 66 degrees centigrade.

The cable sample above described was then bound by hand with heavy duck tape and tested. The maximum force reached was 4,500 pounds for a final conductor temperature rise of 59 degrees centigrade.

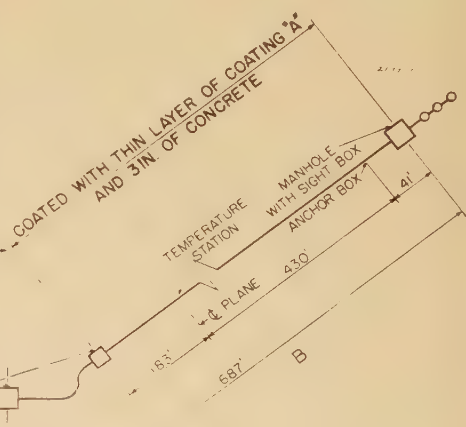
These test results show that when the three conductors of a compression cable are cabled together in a lay of about six feet, but not bound, the longitudinal forces due to restrained thermal expansion are so low that no expansion bends are necessary. This was further verified by the field tests. On the other hand, if the conductors are bound together, either straight or twisted, or kept straight without binding, the forces developed are large enough to endanger joints of usual design.

Outer Coverings

A test was made to determine the ability of duck tape, applied over the outer lead-sheath reinforcing tapes of an unarmored compression cable, to withstand the friction incident to pulling this type of cable into steel pipe. It was observed



A—Unarmored compression cable



B—Armored compression cable

Figure 1. Layout of experimental installation

from laboratory tests that very thin tape was damaged in moving through the pipe even in a short distance of 50 feet. One of the samples used for test was covered with a 20-mil duck tape with salvage. This sample was pulled in and out of the pipe until parts of the sample had been subjected to the same amount of abrasion as would have been received if the cable had been pulled through a 2,000-foot section of pipe. Examination of the sample showed that the heavy duck tape was uninjured.

Experimental Installation

General Layout

The experimental circuit of the compression cable, shown in Figure 1, con-

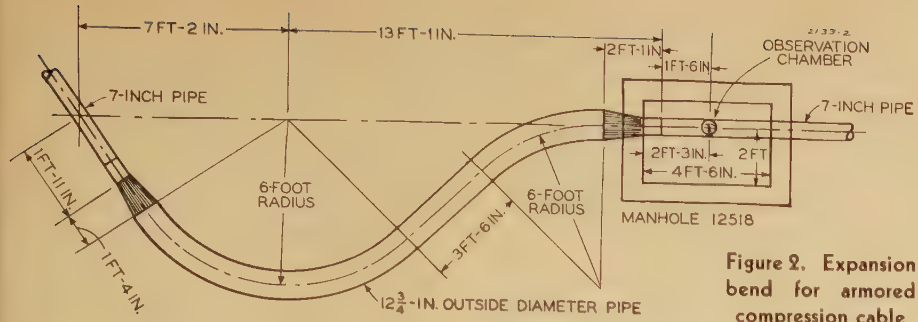


Figure 2. Expansion bend for armored compression cable

sisted of two cable sections each about 700 feet long, one armored and one unarmored, installed in a welded steel pipe. The two sections were connected by means of a joint, and their free ends were provided with terminals for parallel connection with an existing 120-kv overhead line. Suitable equipment was provided for artificially loading the cable by circulating current through the two cable sections with the overhead line providing the return path.

The Pipe Line

Electrically welded steel pipe having an outside diameter of seven inches and shipped in double random lengths was used for this installation. The ends of each length were expanded and beveled for use with chill rings. In the field gas welding was used, and the welds were tested at 500 pounds per square inch air pressure.

The terminal ends were housed in $3^5/_8$-inch outside diameter 0.120-inch wall copper pipes which were fitted and wiped into the trifurcating heads on the ends of the steel pipe line.

The pipe system was provided with nitrogen feeding equipment consisting of a standard cylinder of oil-pumped nitrogen and a gas-pressure regulator with pressure gauges. The line was equipped with a low gas-pressure alarm set for a pressure of 150 pounds per square inch.

The Expansion Bend

An expansion bend was installed near the end of the armored cable in order to protect the joint between the armored and unarmored compression cables from the longitudinal thrust exerted by the armored cable. Figure 2 shows the expansion bend used in this installation, which was made of 12-inch steel pipe with reducers on both ends. The usual practice abroad is to use an expansion bend on each side of the joint.

Corrosion Protection Coverings

The pipe on the unarmored compression cable was given a coating of a material used extensively for pipe covering. This coating, designated as coating *A*, consisted of: a hot application of wax, a spiral wrapping of reinforced asbestos, a hot application of a service coat incorporating natural asphalt, a spiral layer of a single membrane wrapper, and a spiral wrapping of heavy kraft paper. The wax was an adherent coating, chemically and mechanically fortified.

On the armored section concrete was applied around the pipe in the trench to a minimum thickness of three inches.

Cable Design

The armored cable was of conventional design as used abroad. The three 650,000-circular-mil Compack oval conductors were shielded and insulated with 500 mils of impregnated paper. The insulation on each conductor was shielded and covered with 85 mils of lead. There was applied over each sheath three paper tapes, two reinforcing bronze tapes, and two saturated cotton tapes. This assembly was cabled with saturated jute fillers and covered with a heavy saturated duck tape, jute bedding and galvanized steel-wire armor.

The individual legs of the unarmored cable were of the same construction as those in the armored cable, with the addition of two 30-mil presaturated canvas tapes. These legs were cabled without fillers or further finish. Figure 3 shows a sectional view of the unarmored compression cable installed in a steel pipe having an outer protective covering.

Anchoring of the Cables

The armored compression cable was anchored at two points:

1. At the joint with the unarmored cable, to prevent the joint from being moved by the expansion of the cable. Due to proximity of the expansion bend no large forces were involved.

2. At a point approximately 40 feet from the terminal end the armor wires were so anchored as to withstand large longitudinal forces in either direction. The expansion occurring in the 40-foot section was calculated to be negligible and would be taken up by the bend of the cables in the riser pipes. Therefore, at the joint between the terminal ends and the armored cable the armor wires were simply bound in place.

With the above arrangement of anchorage, a longitudinal movement roughly equal to that obtained in a 1,400-foot-long armored compression cable will take place in the direction of the expansion bend and joint between the armored and unarmored compression cables. To observe this movement a removable sight glass with reference mark was installed in the pipe near the end of the expansion bend as well as at other points in the line where such observation would be of use. The cable under each sight glass was equipped with a graduated scale which was fitted in proper position after the cable was placed in its final location.

Normal Joints

Figure 4 shows the normal joint used between the armored and unarmored sections of cable. Figure 5 shows the detailed design for each phase.

The design of the normal joint is of particular interest, because it achieves the connecting of two sections of cable without any appreciable structural change at the point of joining. The design results in a joint that is simple, reliable, and of very small diameter, yet similar in construction to those used on high-voltage single-conductor solid-type cables. Very little schooling is therefore necessary for splicers well-versed in solid-type cable jointing to enable them to construct this joint. Diaphragm action takes place along the joint in a manner similar to that of the cable.

Soldered-type countersunk copper connectors were used with an outside dimension the same as that of the cable conductor. Semiconducting tapes were used to shield the connectors and applied so as to make good contact with the conductor shielding tapes. Long pencils

Figure 3. Sectional view of unarmored compression cable installed in steel pipe having an outer protective covering

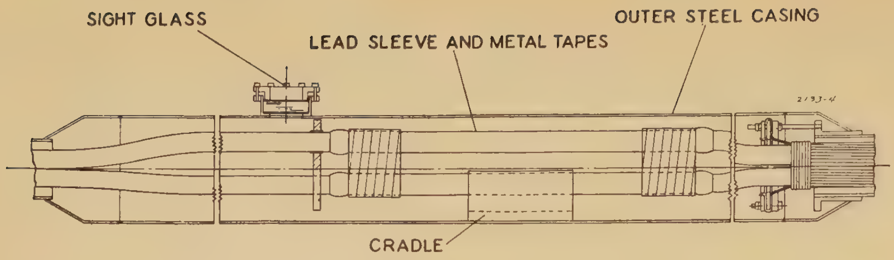


Figure 4. Normal joint between armored and unarmored sections of compression cable

were made on the factory insulation by tearing each tape individually at points one-eighth inch apart. Impregnated-paper tapes were tightly applied, butt-lapped, and wrapped in the same direction, until the thickness of the hand-applied insulation exceeded that of the factory insulation by 20 per cent. Shielding braid was applied over the insulation. The diameter was so made that it required considerable force to draw the oval sleeve over the joint core. The joint core was coated with cable oil before forcing the sleeve in place. This made the sleeve over the joint insulation fit as snugly as the regular sheath over the factory-applied insulation. The sleeve was then beaten down at the ends and wiped to the cable sheath. It was then reinforced with brass tapes similar to the reinforcing on the cable proper. After the individual joints were completed, they were tied together, the steel joint casing pulled over and welded in place.

Laboratory tests had been made previously on two joints of this design. The man making these joints had had no previous experience in making paper-taped joints other than a single similar practice joint. The voltage program as specified in the Association of Edison Illuminating Companies Specification for Oil-Filled Cable was used with the time reduced for the first step to $1^1/_2$ hours, and the 20 per cent increase in voltage every three hours obtained in small steps every ten minutes instead of one large step at three-hour intervals. Breakdown voltages, expressed in volts per mil of cable insulation, of 436 and 445 were obtained. In both joints the breakdown was radial and over the connector; there were no other signs of stressing. These results compared favorably with values obtained on standard normal oil-filled cable joints of this voltage class. These laboratory values are probably very conservative as compared with what might be expected from the field joints where a more nearly circular connector was used, and where the tapes were applied with greater tightness by experienced splicers.

Terminals

Figure 6 shows the design of the terminals used on the experimental line. These terminals have a brass-fitted bakelite stop-tube assembly of sufficient strength to withstand the high internal operating pressure of the cable oil. This stop-tube assembly ends in a stem at the upper end and a flange at the lower end. The porcelain insulator, with a barrier assembly and stress cone, metal rings at top and bottom, and an insulator cap, is placed over the stop tube and bolted to the flange. The space between the stop tube and the porcelain is filled with oil. The terminal is essentially the same design as that used for oil-filled cable with the addition of the stop tube.

Compensators

Compensators were used in order to allow for the expansion and contraction of the impregnating oil contained in the rigid stop tubes of the terminals, and thereby prevent overworking the sheath near the ends of the compression cable. The compensator may be described as a flexible membrane in a housing which is capable of withstanding 200 pounds per square inch pressure. The membrane separates the housing into two chambers; one containing oil is in direct communication with the inside of the stop tube, and the other containing nitrogen is connected to the pipe line. Expansion of the oil in the stop tube and cable due to increasing temperature is accommodated by the movement of the membrane which is spring-loaded to provide a characteristic similar to that of the cable sheath. During cooling the process is reversed, and thus complete impregnation of the terminal at all times without appreciable interchange of oil between the terminal and cable is insured. One compensator is connected to each terminal.

Installation

Methods usually employed for pulling cables into ducts were readily adapted to the installation of the compression cable in the steel pipe. At the start of the installation of the armored compression cable, high pulling stresses (16,000 pounds) were experienced on the pulling line when it passed around the expansion bend located at the pulling end of the section. As soon as the armored cable started to pass through the expansion bend, the pulling stresses on the line became normal.

A maximum pull of 7,800 pounds was experienced during the installation of the unarmored cable. This corresponds to a coefficient of friction of about 0.54. For future installation of unarmored compression cable where an even lower pulling stress is desired, a half-round copper wire wound over the fabric tapes of the individual cables can be employed.

In order to determine the effect on a

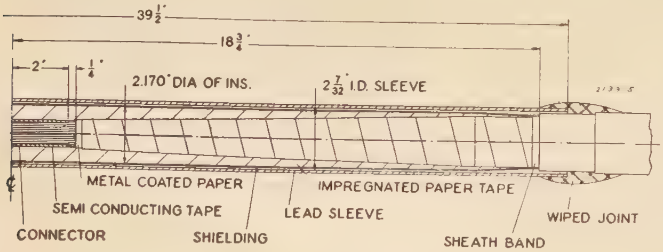


Figure 5. Detail for one conductor of the normal joint

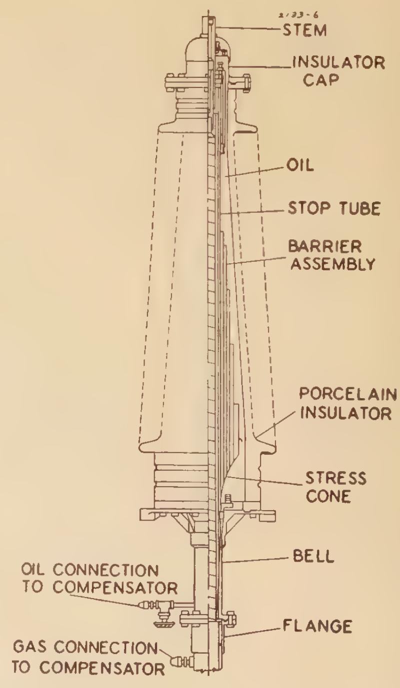


Figure 6. Terminal for 120-kv compression cable

compression cable of nitrogen coming in contact with the insulation, the lead sheath of one phase of the unarmored cable was perforated with holes approximately one-eighth inch in diameter at 25-foot intervals throughout its length.

Before the cables were pulled into their respective pipe sections, the piping was dried with hot air. After the cable was installed the pipe was evacuated and then filled with nitrogen at a low positive pressure. With the help of temporary seals, this nitrogen was kept in the pipes until the cable-splicing operations were completed, at which time the pipe was filled with nitrogen gas at a pressure of approximately 200 pounds per square inch. The nitrogen used was oil-pumped, guaranteed to contain less than 0.3 per cent oxygen and 0.03 per cent water.

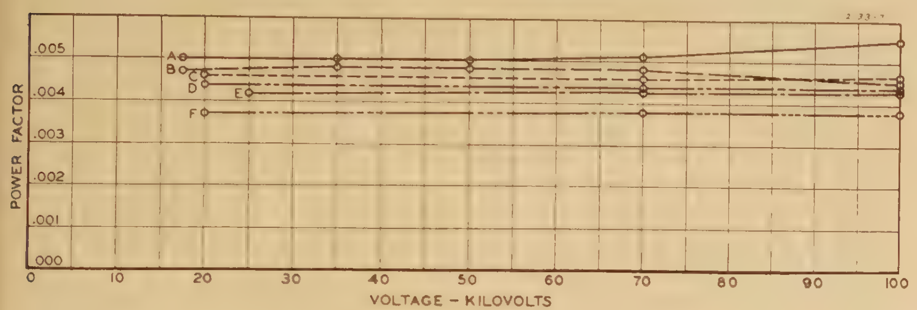


Figure 7. Curves of power factor versus voltage

Curve	Date (1941)	Cable Temperature (Degrees Centigrade)
A	March 29	2.6
B	May 7	14.0
C	May 17	14.8
D	May 27	19.2
E	July 2	20.9
F	August 6	23.5

Tests on the Cable Before Installation

As the compression cable before installation is essentially a standard solid-type cable, routine factory acceptance tests, usually performed on solid-type cable of similar insulation thickness, were considered adequate for detection of nonuniformities. The impulse strength of compression cables had been determined by previous tests,[2] and calculations showed that this cable would fail at a voltage in the neighborhood of 1,100 kv. A sample of compression cable used in this installation withstood without failure impulse voltages up to 900 kv (the capacity of the generator). This is well above the 138-kv insulation level.

Field Tests

After the installation had been completed the cable was subjected to repeated load cycles, and periodic measurements were made of the power factor and movement of the cable within the pipe.

During the time that the cyclic loading tests were in progress (a little over five months), the cable line was subjected to 54 load cycles with load on for $2^1/_2$ hours and load off for $2^1/_2$ hours, and 249 load cycles with load on for 4 hours and load off for 4 hours. In addition, a number of cycles was used in which the load was applied for durations ranging from approximately 13 hours to 190 hours. During the majority of the load cycles, the current ranged from 500 to 550 amperes (or 10 to 20 per cent above the nominal 95,000-kva rating of the line); during others, particularly those of the longer duration, the currents ranged from 460 to 510 amperes.

The first few heating cycles produced a movement of seven-eighths inch in the armored cable at the entrance to the expansion bend over 613 feet of cable and a slight initial adjustment of position of the unarmored cable. Subsequent heating and cooling cycles produced a cumulative $3^3/_4$-inch movement of the armored cable and no appreciable movement (one-eighth inch) of the unarmored section. This indicated that without armoring, the cables would absorb the longitudinal movement, thus making expansion bends unnecessary. During the recent cold season and without load, the armored cable has returned to its original position.

Power-factor measurements were made on each conductor of the two connected sections at voltages from 20 kv to 100 kv to ground. An initial set of measurements was made before starting the load cycles, and subsequent measurements made at intervals during the period that the cable was under test. It was not practical to obtain the power factor of the test cable at elevated temperatures due to the relatively long time necessary to switch the test cable out of the circuit and connect up the power-factor measuring equipment. The measurements were made after the cable had been de-energized for a period of 15 hours or more.

These power-factor measurements as obtained on one of the conductors are shown in Figure 7. There was no significant difference in the measured values for the different conductors. It will be seen that there is a slight decrease in power factor in the later readings which were at slightly higher temperatures than the initial set.

No noticeable difference in power factor was apparent in the phase with holes in the sheath as compared to the other two phases. However, this cable has not been operating long enough to prove this point definitely.

Summary

1. Methods normally used for pulling cables into ducts can readily be adapted to the installation of compression cable in steel pipe.

2. Conditions in this country make it possible to eliminate the armor which is used on compression cable abroad.

3. By cabling the individual lead-covered and reinforced conductors with a suitable lay, longitudinal expansion becomes of no practical importance, and expansion bends as used abroad can be eliminated.

4. Diaphragm action of the lead sheath when in nitrogen shows indefinite life under simulated operating conditions.

5. Compression cable is well suited for handling loads in the range of 100,000 kva.

6. The experimental installation which has been installed for approximately one year shows no deterioration of the insulation even on the phase with the lead sheath perforated every 25 feet.

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2. Impulse Strength As a Measure of Cable Quality, L. I. Komives. AIEE Transactions, volume 60, 1941, October section, pages 929–33.

120-Kv High-Pressure Gas-Filled Cable

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THIS paper deals with the theory, manufacture, and testing of high-pressure gas-filled cable and describes an experimental installation by the Detroit Edison Company in co-operation with the General Cable Corporation and a subsequent commercial installation on the Detroit Edison system.

While high-pressure gas-filled cable has been in successful operation in England, these are the first experimental and commercial installations in the world of this type cable in a steel pipe. The installation of such cable in a steel pipe involves the removal of the lead sheath in the field, thus exposing the insulation to the atmosphere and to the possibility of mechanical injury during installation. Since the effect of such conditions is difficult to determine in the laboratory, the experimental installation was made.

The commercial line was subsequently installed on the Detroit Edison System and put in operation on December 31, 1941. This line is seven miles long and designed to transmit 95,000 kva continuously at 120 kv.

Experience Abroad

High-pressure gas-filled cable has been in successful operation in England since 1937 at which time installations of cable of this type were made for operation at 33 kv and 132 kv. Today there are approximately 25 conductor miles in successful operation at voltages ranging from 33 kv to 132 kv and at pressures from 50 to 225 pounds per square inch. These cables[1] are equipped with lead sheaths reinforced so as to withstand the operating gas pressure.

The high-pressure gas-filled cable makes effective use of the well-known fact that the dielectric strength of gas increases very greatly with pressure. The great gain possible by the use of high gas pressure in cable insulation has long been recognized. However, it was not until a more complete knowledge of other facts became available that effective use could be made of this principle.

The low permittivity and dielectric loss of unsaturated paper made the use of this dielectric seem most promising. Many of the expected results were realized in cables made in this way; dielectric losses were very low, and there was no ionization at voltages well above operating voltage. However, the short-time and impulse breakdown voltages were very low. Since there was no measurable ionization at voltages just below failure it seems that complete breakdown in such a cable follows promptly after the smallest amount of ionization.

An entirely satisfactory solution of the problem has been found by combining several features. The use of high-density low-porosity paper presaturated with a high-viscosity compound has eliminated the quickly destructive effect of ionization produced by overvoltage. These features, together with shielding of the conductors as advocated by two of the authors for all very high-voltage cable,[2] and with the use of very thin tapes near the conductors, have resulted in obtaining an insulation entirely free from ionization up to about twice operating voltage and a dielectric strength (short-time or long-time) in the same range as for oil-filled insulation. Perfection of methods of drying and saturation, the use of a saturant of very low loss, and the prevention of moisture absorption during taping have also resulted in extremely low dielectric loss and a high degree of uniformity throughout the thickness of the insulation.

Two different types of saturant have been used abroad. In the earlier ones, the saturant contained a very large percentage of rosin and had a very high viscosity. However, it also had a high power factor and for this reason was not considered entirely satisfactory. In later cables the saturant has been petrolatum. This has a low loss and does not migrate at a temperature below its melting point of 50 or 55 degrees centigrade, but does become very fluid at temperatures at which cables may operate in this country. Thus neither of these saturants was entirely satisfactory particularly for a pipe cable installation. The saturant used in the Detroit cables has a very high viscosity, possesses extraordinarily good stability, and gives a cable of as low dielectric loss as for the best of modern power cables. The good results obtained on these cables are attributable in no small part to the development and use of this saturant.

The value of the very thin tapes and of the conductor shielding lies primarily in the increase of specific dielectric strength of gas in very thin films. Because of the lower dielectric stress farther from the conductor, the thin tapes are required only near the conductor. Thus by grading the paper-tape thickness, the same dielectric strength is secured for the cable as if all the paper were thin; at the same time there is secured greater freedom in the penetration of gas and more rapid equalization of the pressure through the insulation wall. Moreover, the resulting cable withstands without damage the normal bending in manufacture and installation, as it could not do so fully if made entirely of the thinnest paper.

Nitrogen has been selected as the pressure medium, because of its good dielectric properties, its complete inertness with respect to the materials used in the cable, and its commercial availability with low moisture and oxygen content.

The SMD Type Cable

The previous paragraphs dealt with the basic principles behind the high-pressure gas-filled cable and apply to this type cable as manufactured in England and to the pipe type of cable manufactured in this country and hereafter designated as *SMD* type. The difference in construction is in the outer enclosure. Abroad a reinforced lead sheath is used over the insulated conductors to provide this gas-tight enclosure. An additional lead sheath is usually employed to protect the reinforcements. The *SMD* type of high-pressure gas-filled cable retains the essential insulation of the reinforced lead-sheath type but does away with the permanent lead sheaths as used abroad. All three phases of even the largest-size cables are installed in one common pressure chamber. This pressure chamber is a steel pipe which is in itself gas-tight, and which is designed to withstand the required pressure. A lead sheath which

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serves as a protective covering during shipment and storage is removed as the cables are installed.

While the insulation of the *SMD* type cable is the same as for the reinforced sheath type, the removal of the lead sheath requires that a covering be placed over the insulation for protection during installation. In the cable used in the experimental line this was accomplished by the application of a strong canvas tape over the insulation shielding tapes. The cable for the commercial line was manufactured with a half-round copper wire over the canvas tape to increase the mechanical protection and reduce the coefficient of friction between the cable and pipe.

Since the installation of this type of cable requires the removal of the lead sheath in the field, the exposure of the insulation to the atmosphere, and the possibility of mechanical injury during installation, it was felt that an experimental installation was essential to demonstrate the practicability of installing this type of cable in a commercial line.

While the superiority of insulation made with graded tapes of five mils and less was recognized, the question of whether or not an insulation built from papers of standard thickness, five mils or over, would be satisfactory was raised. To obtain an answer to this question, it was decided to employ all five-mil tapes in the *SMD* type cable used in the experimental line. The five-mil thickness was used to facilitate taping and to depart as little as possible from the standard practice used in the manufacture of power cables. In the cables built subsequently for the commercial *SMD* line, grading of paper thickness was employed, thus conforming to the practice abroad.

Manufacture

The cable in the commercial installation was of the following construction: single conductor 600,000 circular mils concentric round strand, 600 mils preimpregnated wood-pulp paper of graded thickness from $2^1/_2$ to 5 mils, two metal-faced paper tapes applied next to the conductor, and two metal-faced tapes over the insulation (all metal-faced tapes included in the 600-mil wall thickness), bronze shielding tape intercalated with a saturated muslin tape, one 20-mil paraffin saturated tape, one half-round 200- by 100-mil copper wire applied in open spiral, and a 94-mil temporary lead sheath. Figure 1 shows a sectional view of this cable installed in the steel pipe having an outer protective covering.

Before being applied to the cable, the paper tapes were dried and saturated with an oil having a viscosity of 3,000 seconds Saybolt Universal (50 centipoises) at 100 degrees centigrade. The preimpregnated-paper tapes were wrapped on the conductor in a standard taping machine and in the usual manner, except that the entire machine was enclosed in a room where in the relative humidity was maintained below 20 per cent regardless of outside atmospheric conditions. This humidity control, together with precautions taken during impregnating, handling, and storing, greatly reduced the moisture content in the finished cable.

The cable for the experimental installation differed from the above only in the omission of the half-round copper wire, the use of five-mil paper tapes throughout, and the application of paper tapes without humidity control.

Specification

SMD type cable may be tested in accordance with the Association of Edison Illuminating Companies Specification for Oil-Filled Cable, provided that during the electrical tests an internal nitrogen pressure of 30 pounds per square inch is maintained on commercial lengths and 225 pounds per square inch on samples. The temporary lead sheath on the commercial lengths should, of course, be of sufficient strength to withstand this pressure.

Experimental Installation

The experimental circuit of *SMD* type cable consisted of one section of cable of approximately 700 feet in length with normal joints at each end connecting to the terminal end cables. Terminals were provided at each end for connection with

Figure 1. Sectional view of SMD type cable installed in a protective-coated steel pipe

an existing 120-kv overhead line. The cable was installed in a seven-inch-outside-diameter welded steel pipe having a wall of 0.231 inch and filled with nitrogen gas at a pressure of approximately 200 pounds per square inch. The pipe section housing the cable was provided with manholes at each end at which points sight boxes were provided for observing cable movement.

Two different types of protective coverings were applied to the pipe. About half of this 700-foot section was covered with a coating of plasticized coal-tar enamel. Joints in the pipe were hand-coated with the same material. The balance of this section of pipe was covered with Somastic. This material consists of a primer coat over which was extruded, hot, a half inch of asphalt mastic material.

The joints and terminals were of the same general design as used on the commercial line and are described hereafter.

Since it was desirable not to expose the insulation to the atmosphere any longer than necessary, a mechanical stripping device was designed which folded the lead sheath back as the cable was pulled into the pipe. V-shaped grooves were made in the lead sheath during the lead covering process in order to facilitate stripping. It was found, however, that scoring of the lead sheath by hand in the field was still necessary, and that mechanical stripping increased the pulling stresses considerably. It was thus unnecessary to score the lead at the factory, as scoring in the field was all that was required to facilitate this operation.

Field Tests

Measurements were made after the installation had been completed in order to determine what effect cyclic loading of the cable would have on the power factor of the insulation and what movement of the conductors would take place.

The cable which was connected to the 120-kv overhead line was subjected to load cycles by the circulation of current through the conductor with the overhead line acting as the return path. During the first 54 cycles the load was on for $2^1/_2$ hours and off for $2^1/_2$ hours. This was followed by 249 cycles of longer duration with the load on for 4 hours and off for 4 hours. In addition there were a number of cycles in which the load was applied for durations ranging from 13 to 190 hours. For the majority of the load cycles the current ranged from 500 to 550 amperes (or 10 to 20 per cent above the nominal 95,000-kva rating of the line). The maximum change in copper temperature

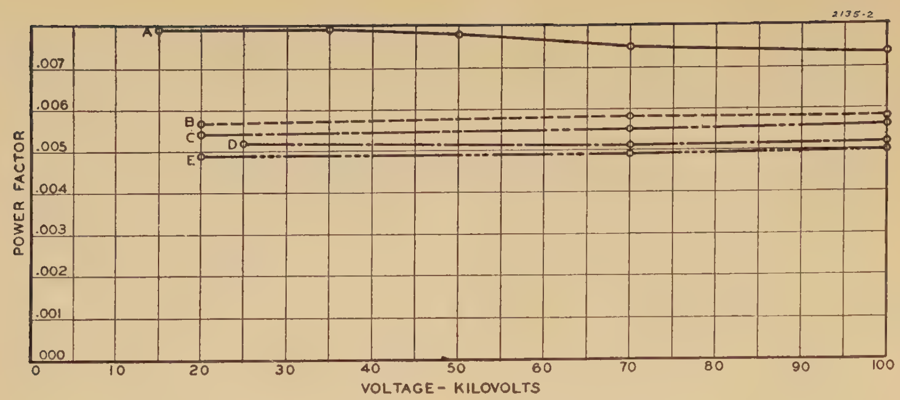


Figure 2. Curves of power factor versus voltage for experimental installation

Curve	Date (1941)	Cable Temperature (Degrees Centigrade)
A	March 29	2.6
B	May 17	19.6
C	May 27	22.3
D	July 2	25.8
E	August 6	27.7

that occurred during this period of loading was 58 degrees centigrade.

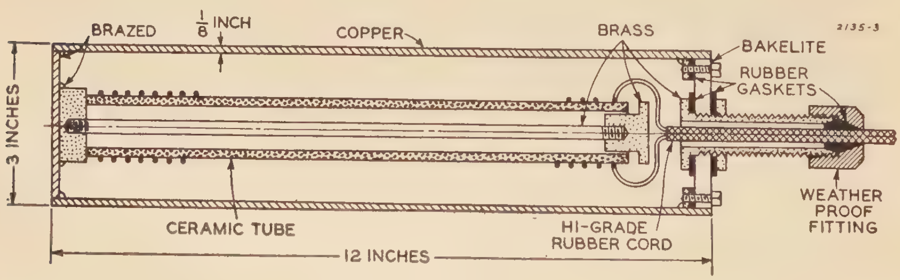
The above load cycles produced only a slight initial adjustment in the position of the cable, and no further movement of the cable occurred thereafter. This indicates that there is sufficient space in the pipe to absorb the longitudinal thermal expansion.

Power-factor measurements were made on each of the three conductors at voltages from 20 kv to 100 kv to ground. An initial set of measurements was made before loading and repeated at intervals during the period that the cable was being subjected to the loading cycles. The measurements were made after the load had been removed from the cable for a period of 15 hours or more. It was not feasible to obtain power-factor measurements at elevated temperatures, since it required a relatively long time to switch the experimental cable out of the circuit and connect up the power-factor measuring equipment.

The power-factor data as obtained on one of the conductors are shown in Figure 2. There was no significant difference in the measured values for the different conductors. It will be noted that the later power-factor values are noticeably lower than the earlier ones. The first tests were made at 2.6 degrees centigrade, and the last one at 27.7 degrees centigrade. This is entirely consistent with laboratory data, which show that in the lower range of temperatures the power factor of the insulation decreases as the temperature increases. These measurements show no evidence of deterioration of the insulation.

Figure 3. Cell for measuring thermal resistivity of soil

Ceramic tube wound with Chromel wire to dissipate 200 watts at 120 volts



Soil-Resistivity Measurements

During the period that tests were being conducted on the experimental installation, soil-resistivity measurements were made for use in designing the commercial line. Measurements were made to determine the thermal resistivity of the earth under varying seasonal conditions and for different types and conditions of soil. A cell was used consisting of a copper cylinder twelve inches long and three inches in outside diameter, with a one-eighth-inch wall, and equipped with an internal electric-heating element. This heater was capable of dissipating 200 watts at 120 volts, uniformly over the walls. A copper-constantan thermocouple was embedded in the mid-point of the cylinder wall. The general construction of this cell is shown in Figure 3.

This cell was buried vertically in the ground; the heater supply and thermocouple leads were brought out to a convenient point. Means were also provided for measuring the ambient earth temperature nearby. Sufficient energy was supplied to the heater element to cause a 40- to 50-degree-centigrade rise between cylinder wall and ambient earth. For the soils so far tested an input of 50 watts has been necessary.

The earth thermal resistivity (in degrees centigrade per watt per centimeter cube) may be computed from the formula $g=(128/W)(T_1-T_2)$: when W = watts input, T_1=temperature of cylinder wall, and T_2=temperature of ambient earth. This formula is empirical and was derived by comparison with the results obtained from such a cylinder with those obtained with a sphere.[3]

Figure 4 shows values of resistivity for three types of soil encountered along the route of the commercial line, as well as the variations in resistivity that are caused by the changes in moisture content during the different seasons of the year.

Tests with the thermal resistivity cell at what will probably be the locality where the soil has the highest thermal resistivity along the commercial line (in fine sand) show maximum average values of 120 during the dry season. In other areas where loam prevails, maximum average values of 95 and 75 were measured during the dry and wet seasons, respectively. Corresponding measurements where heavy clay prevails show resistivities of 105 and 95, respectively.

Commercial Line

General Layout

The commercial line consists of a seven-mile circuit installed under paved city streets. The cable used has previously been described under "Manufacture" and was shipped and installed in lengths of approximately 1,500 feet. The pipe line consisted of seven-inch steel pipe protected from corrosion by means of Somastic pipe covering. The line is sectionalized by stop joints, one near each termination and four at intermediate points, thus dividing the line into five approximately equal sections. These stop

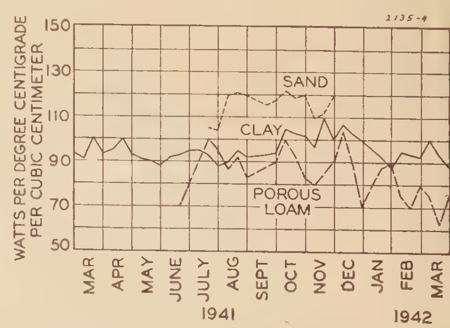


Figure 4. Seasonal soil-resistivity curves

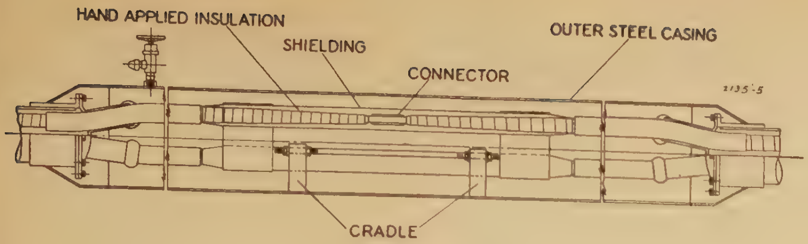


Figure 5. Normal joint for *SMD* type cable

joints are provided so that if repairs to the steel pipe are required, because of corrosion or for other reasons, it will be necessary only to discharge the nitrogen from the section involved. Sectionalizing the line also facilitates the location of a gas leak in the steel pipe if a leak should occur. In normal operation the stop joints are by-passed by a pipe containing a valve which may be closed for sectionalization. Nitrogen tanks were connected through pressure regulators to the ends of the pipe to take care of any leakage. A low pressure alarm was installed to operate when the gas pressure drops to 150 pounds per square inch.

Installation

The temporary lead sheath was scored and stripped by hand, as the three cables were pulled as a unit into the steel pipe. No difficulty was encountered in pulling these 1,500-foot lengths. The maximum pulling stress that occurred on any of the sections was 7,200 pounds which corresponds to a maximum coefficient of friction of 0.37.

For a distance of approximately 15 feet on each end of the cable sections, the lead sheaths were allowed to remain to facilitate making the joints, and to permit sealing the cables in the steel pipe during installation. The cables were sealed in the pipe by means of trifurcating heads, which consisted of a flange and gasket bolted to the pipe ends and three lead nipples wiped to the cable sheaths.

Previous to pulling in the cable, each section of pipe line between manholes was tested for leaks with 500 pounds per square inch air pressure for 24 hours. The pressure was then released, and the pipe filled with dry nitrogen gas at a pressure of 5 to 10 pounds per square inch. This pressure was maintained until the cable was pulled. After the cable was pulled, the ends of each section were sealed by means of the trifurcating heads, and dry nitrogen gas admitted at a pressure of 5 to 10 pounds per square inch. After each section between stop joints was completed, it was evacuated. When all sections of the line were completed, the line as a unit was filled with dry nitrogen gas at 200 pounds per square inch.

Normal Joints

The normal joints, shown in Figure 5, are essentially the same in the experimental and commercial lines and were hand-taped with presaturated paper. Soldered-type countersunk copper connectors having a diameter the same as that of the cable conductor were used. These avoid the regions of high stress that occur near the ends of standard connectors. Stress control was further improved by the use of semiconducting tapes for shielding the connectors. These tapes were applied so as to make good contact with the conductor shielding tapes. Long pencils were made on the factory insulation by tearing each tape individually at points approximately one-eighth inch apart. The paper tapes were applied butt lap to a thickness 20 per cent greater than that of the factory-applied insulation.

The hand-applied insulation was tapered at the ends of the joint by tearing the successive tapes at points $^9/_{16}$ inch apart. The joint was completely shielded with tinsel copper braid. Special rubber sleeves were temporarily pulled over the completely insulated and shielded joints to protect them against dirt and moisture and to permit maintaining low gas pressure on the insulation. The ends of these rubber sleeves were sealed with rubber tape to the short sections of lead sheath left on the cable ends. After all three joints were completed, the rubber sleeves were removed, the wipes at the trifurcating heads melted off, the trifurcating heads moved two inches from the pipe ends and the wipes remade. This procedure permitted entrance of gas into the joint casing and anchored the cables at the joint. The enlarged section of pipe forming the joint casing was then pulled over the joint and welded in place.

Stop Joints

The stop joint, shown in Figure 6, is essentially the same as those used for oil-filled cable. The stop tubes have somewhat greater wall thickness to give strength to withstand safely a differential pressure of 225 pounds per square inch. At one end the three stop tubes are sealed to a diaphragm through which the cables pass. This diaphragm is sealed to the outer joint casing. The condenser unit for each of the three individual joints is similar to the condenser for oil-filled cable except that it is wound with preimpregnated paper, and no individual oil-tight outer sleeves are used. This allows the gas to come into direct contact with the insulation of the condenser, thus improving its dielectric strength in the same manner as for the cable proper.

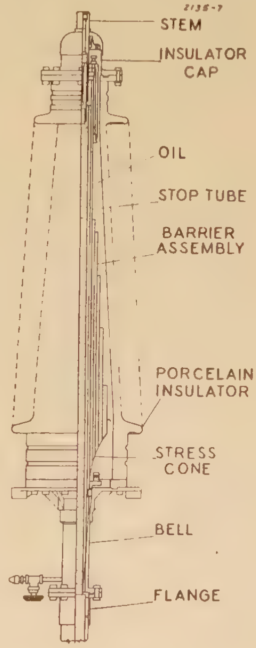


Figure 7. Terminal for *SMD* type cable

Terminals

The terminals used on both the experimental and commercial lines have a brass-fitted bakelite stop-tube assembly similar to those used in the stop joints but of greater length. This stop tube assembly ends in a stem at the upper end and a flange at the lower end. The porcelain insulator, with a barrier assembly and stress cone, metal rings at top and bottom, and an insulator cap, is placed over the stop tube and bolted to the flange. The space between the stop tube and the

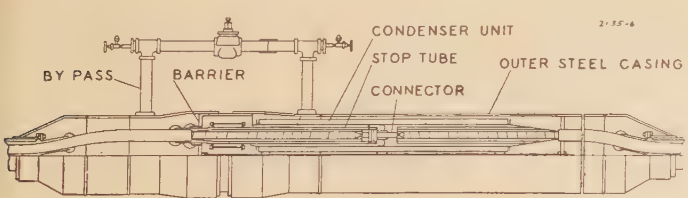


Figure 6. Stop joint for *SMD* type cable

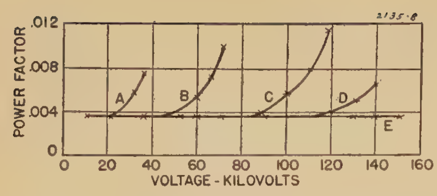


Figure 8. Ionization tests at different gas pressures on 600,000-circular-mil cable with 600-mil insulation

Curve	Pounds Per Square Inch
A	0
B	50
C	100
D	150
E	200 and 225

porcelain is filled with oil. Thus the terminal is essentially that used for oil-filled cable with the addition of the stop tube. The terminals are installed in a manner similar to single-conductor terminals on oil-filled or solid-type cables. Figure 7 shows the design of these terminals.

Manholes

The 24 normal and 6 stop joints were housed in manholes having brick walls, concrete floors, rail-supported brick ceilings, two chimneys, and built-in pulling eyes. The normal joint manholes were 12 feet long, 4 feet 9 inches wide, 5 feet high, and those housing the stop joints were 17 feet 10 inches long, 5 feet 6 inches wide and 5 feet high. Corrugated galvanized-iron culverts, concentric with the pipe, were used for storing the steel-joint casings out of the way of the splicers during construction. One such culvert was provided in the normal-joint manholes and two in the stop-joint manholes.

Laboratory Tests

Power Factor Versus Pressure and Voltage

The effect of different gas pressures on the power factor at different voltages is shown in Figure 8. This cable operates at approximately 70,000 volts to ground and was designed to show no measurable ionization at double working voltage, or 140,000 volts, when under a nitrogen pressure of 225 pounds per square inch. In general, there was no appreciable ionization at 140,000 volts and 200 pounds per square inch. It is apparent also that at approximately 80,000 volts and 100 pounds per square inch, no appreciable ionization occurs. Thus the minimum safe operating gas pressure for this cable is approximately 100 pounds per square inch. Ionization data on this lot of cable may be summarized as indicated in Table A.

Table A

Gas Pressure (Lb. Per Sq. In.)	Voltage at Which Ionization Begins
Atmospheric	Approximately 23 kv
50	Approximately 50 kv
100	75–90 kv
150	100–120 kv
200	120–140 kv
225	140 kv or above

Compound Migration

The fact that this cable was to be installed in a steel pipe without a lead sheath made it necessary to determine whether at high temperatures the impregnant would flow out of the paper into the pipe, or when installed vertically, the compound would flow longitudinally of the cable. If such migration would be harmful, the effect would become apparent from ionization tests. In Figure 9 are curves showing ionization tests on a sample of cable after heating for 65 days at 100 degrees centigrade in an inverted U position. The sample in the bent position had an over-all height of 15 feet of which only a very small part was consumed by the storage of the small amount of drained compound at the lower ends in the space between the lead sheath and insulation. This offered as complete an opportunity for drainage as for a cable of any height. The cable was allowed to cool for eight hours each day for 38 of the 65 days. Initially and also after 43 days' heating there was no ionization at 140 kv and 225 pounds per square inch, but after 65 days' heating ionization started at 130 kv. Thus it is safe to assume, since there was no change after 43 days' heating at 100 degrees centigrade, and only a slight change after 65 days' heating, that migration is not a problem at normal operating temperatures even in vertical runs.

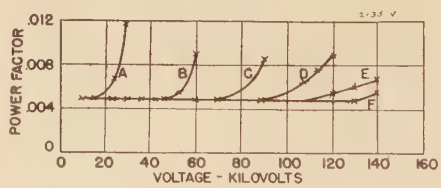


Figure 9. Ionization tests at different gas pressures on 600,000-circular-mil cable with 600-mil insulation, after being heated for 65 days at 100 degrees centigrade in an inverted-U position

Curve	Pounds Per Square Inch
A	0
B	50
C	100
D	150
E	200
F	225

Radial Power Factor

It has been customary in the industry to measure radial power factor at 60 degrees centigrade. In order to exaggerate effects on power factor of small quantities of contaminants, notably moisture, radial power factor tests on *SMD* type cable were made at 80 degrees centigrade. The greater sensitivity of measurements at 80 degrees centigrade, as compared with 60 degrees centigrade, is markedly shown in Figure 10 where curves *C* and *D* show radial power factor at these respective temperatures. The sample of cable used for these measurements was an experimental section made before the procedure used in the construction of this type of cable had been perfected.

Figure 11 shows the very high degree of correlation that exists between the power factor and moisture content. The radial moisture-content curve has been superimposed on the radial power-factor curve, and the variations in the moisture content are closely followed by variations in the power factor. These measurements were made on samples taken from the section of experimental cable mentioned as having been made before the manufacturing technique was perfected. To obtain an accurate measure of moisture content, groups of tapes were used instead of single tapes. Tests made on a number

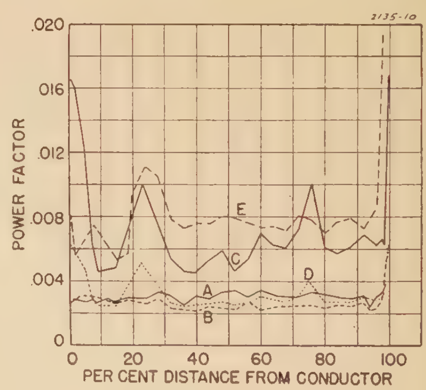


Figure 10. Radial power factor on experimental and commercial cables

A—New cable, "commercial," measured at 80 degrees centigrade

B—Same cable as *A* after 113 days at 100 degrees centigrade

C—New cable, "experimental," measured at 80 degrees centigrade

D—Same cable as *C* measured at 60 degrees centigrade

E—Same cable as *C* after 99 days at 100 degrees centigrade, measured at 80 degrees centigrade

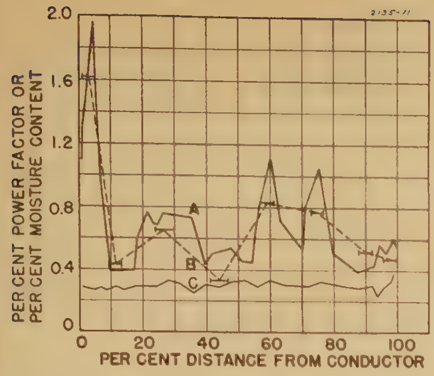


Figure 11. Radial power factor and moisture content of experimental cable

A—Radial power factor on "experimental" cable, measured at 80 degrees centigrade

B—Radial moisture content on "experimental" cable

C—Radial power factor on "commercial" cable, measured at 80 degrees centigrade

of samples show that when the moisture content is below one half of one per cent, it has no significant effect on power factor.

A very noticeable improvement in the 80-degree-centigrade radial power factor took place after the room humidity was maintained below 20 per cent during the taping operations. This improvement is demonstrated by the comparison of the radial power factor curves *A* and *C* shown in Figure 11 for cable made before and after the adoption of humidity control.

Power Factor—Temperature Characteristic

Figure 12 shows a typical power factor versus temperature curve for *SMD* type cable made by the latest practice. This compares very favorably with the best of other types of cable insulation, including that used on oil-filled cable.

Temperature Stability

The temperature stability of the insulation is as important for *SMD* type cable as for other types. The excellent temperature stability of the cable made for commercial use is shown by curves *A* and *B* of Figure 10. Curve *A* shows the radial power factor at 80 degrees centigrade for the new cable, and curve *B* shows the same characteristic on another sample of the same cable after 113 days at 100 degrees centigrade. In both cases the power-factor curve is essentially flat for the full section of the insulation. The aged sample shows no indication of an increase in power factor; in fact, this sample shows a somewhat lower average power factor than the unaged sample. Other samples show no appreciable change in over-all power factor after aging for 162 days at 100 degrees centigrade. Curves *C* and *E* show a similar radial power-factor comparison of "experimental cable," which was manufactured before moisture control was perfected.

Dielectric Strength

The results of dielectric-strength tests on thirteen 30-foot samples taken from the cable made for installation in the commercial line are shown in Figure 13. These samples were tested at a gas pressure of 225 pounds per square inch. There are shown, also, two tests on cable tested at 30 pounds per square inch pressure, one test on cable drained before voltage application, and one test on cable at high temperature. It will be observed that, for tests of a duration between about 1 hour and 100 hours, the approximate life to failure varies inversely as 7.5 power of the voltage. There is little or no decrease of strength for periods above 100 hours. For periods less than one hour the strength increases much less rapidly with decreasing time than in the range between 1 and 100 hours. The fact that the dielectric strength for a period of a few hours is reduced no more than about 10 to 20 per cent when tested at 30 pounds per square inch instead of 225 pounds per square inch is rather remarkable. Drainage under very severe conditions, that is, with the cable supported in an inverted U position and maintained at a temperature of 100 degrees centigrade throughout the cross section for a period of 44 days, reduced the strength 10 per cent when tested at a voltage which produced failure in 125 hours. The sample tested hot was held at 75 degrees centigrade by circulating current in the sheath of the cable for about 1,250 hours. Voltage was maintained on it, while hot, for about 530 hours at values increasing from 120 kv to 165 kv. The result of this test was plotted as the equivalent of 108 hours at 160 kv. Even on this basis the test was rather better than the average of the cables tested without heating. It is also to be noted that the power factor at 75 degrees centigrade remained at about 0.3 per cent throughout the test with no tendency to increase.

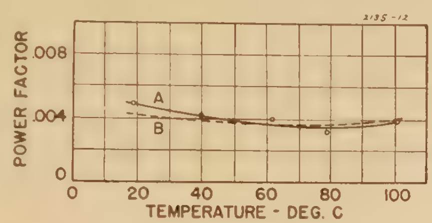


Figure 12. Curves of power factor versus temperature

A—*SMD* type cable "commercial"

B—Typical for oil-filled cable

Figure 8, which shows the power factor versus voltage characteristic, gives a basis for understanding the high long-time strength of this insulation. For a given over-all average stress in the insulation, the actual stresses in the paper and in the oil are lower than in other types of insulation, because of low specific inductive capacity caused by the gas spaces. Thus, as long as there is no breakdown in the gas spaces, it is clear that long-time dielectric strength should be at least as great as for the best of other types of insulation—namely oil-filled insulation. The "ionization" test is one of the best criteria of the beginning of ionization in the gas spaces. It is recognized from extensive tests and engineering experience that the effect of electrical stress in a gas changes sharply and completely at the beginning of ionization (ionization by collision). Above that stress the gas becomes a partial conductor. Even more serious than this is the effect of the bombardment of the solid and liquid insulation at the boundaries of the gas spaces in the gas. This bombardment produces "wax formation," charring of the paper, "coring," and ultimate breakdown. Below the ionization voltage, however, the stress has substantially no effect on the insulation, and, so far as the stress is concerned, the cable should last indefinitely.

A great deal of information has been obtained by dissecting completely (unwinding) the entire length of these many test specimens. In the tests in which complete failure took place after more

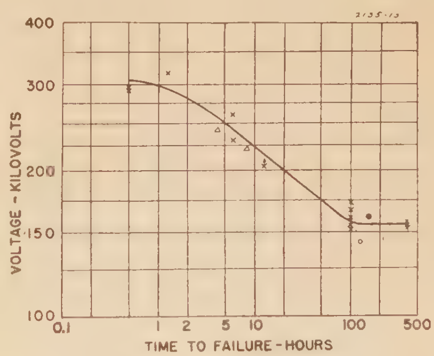


Figure 13. Voltage–time curve

x—Test made at room temperature and 225 pounds per square inch pressure

o—Test made at room temperature and 225 pounds per square inch pressure after the cable had been heated to 100 degrees centigrade in an inverted-U position for 44 days

●—Test made at 75 degrees centigrade and 225 pounds per square inch pressure after 1,250 hours at 75 degrees centigrade

Δ—Tests made at room temperature and 30 pounds per square inch pressure

than 24 hours, there were frequently found one or more partial failures. Because of the small extent of the burning, it was possible to obtain from these partial failures very complete information as to their course. The different partial failures were evidently of similar character but in different stages of development. They originated in the butt space of the tape next to the conductor, usually so close to an edge of the tape as to burn it slightly. When several of these partial failures in different stages were examined, some were found with only a few layers of paper punctured, the size of the hole increasing outwardly from the conductor. In the more advanced stages, the outermost tapes were discolored (brown), usually to a diameter of $^1/_{32}$ inch to $^1/_{16}$ inch, but sometimes to a diameter up to $^1/_8$ inch. In still later stages, "tree design" or "dandrites" appeared at the tip of the advancing puncture. In later stages, these spread radially and outwardly toward final failure. The brown spots were evidently an immediate forerunner of the "tree design," since the first appearance of both occurred when the penetration had reached about five per cent or ten per cent of the total insulation thickness.

Elsewhere than at the complete and partial failures, the insulation seemed only slightly affected by the test. The odor ("mouse-nest odor") which characteristically accompanies insulation tested at high stress was prominent; but no other effects were observed except that on rare occasions traces of wax were found. Several samples were tested after repeated bending produced by rereeling 10 to 15 times around a drum having a diameter of about 16 times the cable diameter. These broke down at 140 kv after durations of 31 to 70 hours. Examination showed that the insulation had been displaced by the handling. This displacement had resulted in regions of wide interturn gaps or valleys between tapes (up to about 0.2 inch as compared with about 0.06 inch before bending) alternating with cross sections where these valleys were practically closed. This characteristic was substantially uniform throughout any cross section. In most of these samples there were several partial failures. All failures and partial failures were in the regions of wide valleys. Most of the cable made for the commercial line was made with slightly smaller valleys and was more resistent to the effect of repeated bending.

Cable handled as severely as required in installation was not affected by the handling. A sample from a length (the leading end of a 1,500-foot length) which had been subjected to excessive pulling strain during installation and had been pulled through about 3,000 feet of pipe, was tested and broke down at about 93 per cent of the voltage of the average new cable.

Table I. Impulse Strength of *SMD* Type Cable for Various Constructions

						Construction			
						Paper Tapes			
Sample No.**	Insulation Thickness (Mils)	No. of Surges	Breakdown Voltage (Kv)	Average Gradient (Volts Per Mil)	Maximum Gradient (Volts Per Mil)	No.	Thickness (Mils)	Density*	Saturant Viscosity† or Type
A-1	600	1	680	1,130	1,800	120	5	0.85	3,000
A-2**	600	1	580	965	1,535				
B-1	600	3	900+	1,500+	2,380+	40	3	1.20	1,000
						24	5	0.85	1,000
B-2	600	3	905+	1,510+	2,390+	Balance	5	0.85	3,000
C-1	600	1	610	1,030	1,615	30	2	1.00	Petrolatum
C-2	600	1	715	1,190	1,880	16	$3^1/_2$	1.00	Petrolatum
C-3‡	600	1	520	865	1,375	Balance	5	0.85	3,000
D-1	600	5	900+	1,500+	2,355+				
D-2	600	6	940+	1,565+	2,460+				
D-9	600	3	980+	1,630+	2,560+				
D-11	600	4	925+	1,540+	2,420+				
D-12	600	3	920+	1,530+	2,400+				
D-13	600	3	900+	1,500+	2,355+	19	$2^1/_2$	1.20	3,000
D-14⊗	450	1	820	1,820	2,620	24	3	1.20	3,000
D-15⊗	450	1	900+	2,000+	2,870+	Balance	5	0.85	3,000
D-16	450	3	925+	2,055+	2,950+				
D-17⊖	385	1	580	1,505	2,085				
D-18	385	1	720	1,870	2,590				
D-19	385	1	775	2,010	2,790				

Plus values after breakdown voltage indicate that no failure occurred on these samples; on all other samples breakdown occurred in the cable.

Terminal failures occurred in seven additional *D* samples.

200 pounds per square inch nitrogen pressure was applied to each sample for one hour except samples *A*-2 and *C*-3.

** Pressure was applied for 60 hours.

‡ Tested at atmospheric pressure.

⊗ These samples were subjected to abnormal pulling stresses in field before test.

⊖ This sample was subjected to abnormal pulling stresses in field before test and visibly damaged.

* Density of paper tapes is the apparent specific gravity—the volume in cubic centimeters of a tightly wound roll of tape divided by weight in grams.

† Viscosity of saturant is given in Saybolt seconds at 100 degrees centigrade.

Impulse Tests

One of the main questions in connection with the *SMD* type cable was whether or not this cable would have a sufficiently high impulse strength if connected to overhead lines or outdoor substations; therefore, impulse tests were performed on samples of this cable. In this paper samples of cable from the experimental line shall be designated as *A*, and samples of cable subsequently produced, each of different construction, as *B*, *C*, and *D*. The *D* construction was used for the commercial installation.

The results of impulse tests on two *A* samples indicated impulse strengths of 680 and 580 kv. These values were not adequate, because a minimum impulse strength of 750 kv was considered necessary to co-ordinate with the 138-kv-class insulation used for the terminals and other apparatus on the system.

Samples designated as *B* and *C* represent two short experimental lengths on which a limited number of 60-cycle and impulse tests were made. On the first of these, satisfactory values of impulse strength were obtained, but the 60-cycle values were too low, whereas on the second type of construction the 60-cycle values were satisfactory, but the impulse strength was too low. Owing to the limited number of tests, the causes of this could not be determined. This became unimportant, however, as tests on cable with the *D* construction indicated satisfactory values both for 60-cycle and impulse tests. Table I shows the results of these impulse tests.

As the maximum potentials produced by the impulse generator did not cause failure in the cable with the improved construction (*D* samples), the actual impulse strength of such cables could not be determined. Breakdowns were obtained, however, on samples on which insulation thickness was reduced by removing a number of tapes from the outside of the insulation. The results of these tests

are shown in Table I, samples D-14 to 19 inclusive. Two reduced insulation thicknesses were selected, so that breakdown voltage values could be conveniently compared with those obtained on other types of cables previously tested.

Figures 14 and 15 graphically illustrate the breakdown voltage values and maximum voltage gradients, respectively, of the samples tabulated in Table I, and compared with the results of previous tests by one of the authors[4] and others,[5,6] on the basis of equal insulation thicknesses. The slanting line on Figure 14 connects minimum breakdown values obtained by the authors and others on oil-filled, compression, Oilostatic, solid, and *SMD* cables, having insulation thicknesses of 385, 450, and 500 mils, respectively. These lines were extended to the full insulation thickness range for the D samples, in order to obtain an indication of the impulse strength which otherwise was not obtainable, because of the 1,000-kv limitation of the impulse generator.

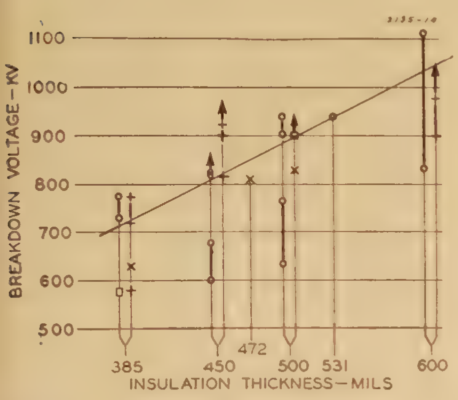


Figure 14. Impulse strength of *SMD* type cable

o—Oil-filled
□—Compression and Oilostatic
X—Solid
+—*SMD*

From Figures 14 and 15 it is evident that on the basis of *average stress* the *SMD* type cable in the commercial line has an impulse strength as high as has been obtained by various observers for well-made oil-filled cable of standard construction and equal insulation thickness. On the basis of *maximum stress* as customarily calculated, without taking into account the effect of stranding, the impulse strength of the conductor-shielded *SMD* type cable is higher than that of unshielded oil-filled cable. It is thought that the conductor shielding of the *SMD* type cable is responsible for this difference, although the extent of the benefit from this shielding has not been directly determined.

With 600 mils of insulation the impulse strength of the *SMD* cable is in excess of 1,000 kv. These tests indicate that 400 to 425 mils of insulation should be adequate for the desired impulse strength of 750 kv.

Summary

1. Nonleaded high-pressure gas-filled cable can be installed in a steel pipe without the insulation being adversely affected due to atmospheric exposure or mechanical handling.

2. No difficulty was encountered in the installation of 1,500-foot section lengths of cable on the commercial line. Based on the relatively low pulling stresses (7,200 pounds maximum), it appears perfectly feasible to install this type cable in lengths up to 2,000 feet.

3. The use of paper, preimpregnated with viscous compound, prevents migration at normal operating temperatures and allows installation on steep grades or in vertical runs without stop joints or other devices.

4. The satisfactory results on this type of cable are due in part to the development and use of a saturant of high viscosity and of excellent electrical properties.

5. The 60-cycle dielectric strength, both short- and long-time, is equivalent to that of oil-filled cable.

6. The impulse strength is at least the equal of oil-filled cable.

7. Power-factor values are approximately constant over a temperature range of 20 to 100 degrees centigrade.

8. The power factor was not affected even after six months at a temperature of 100 degrees centigrade.

9. The radial power-factor curve is flat even at 80 degrees centigrade, where impurities often cause erratic results.

10. Great uniformity of the insulation is indicated by the fact that on long-time voltage tests there are normally numerous partial failures, in addition to the completed failure.

11. The same insulation thicknesses as used on oil-filled cable can be used on high-pressure gas-filled cable, and all oil-fill cable tests met with a gas pressure of 30 pounds per square inch on reel lengths and 225 pounds per square inch on samples.

12. High-pressure gas-filled cable is practical for handling loads of the order of 100,000 kva and above.

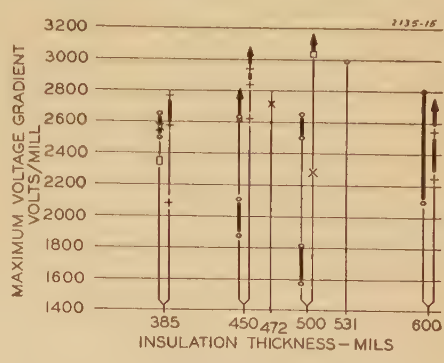


Figure 15. Maximum stress values for the impulse measurements shown on Figure 14

No correction made for the effect of stranding on maximum stress

o—Oil-filled
□—Compression and Oilostatic
X—Solid
+—*SMD*

13. Measured values of thermal resistivity are given for several kinds of soil.

14. High-pressure gas-filled cable with preimpregnated paper can be manufactured on standard equipment, with only minor changes, provided humidity is maintained below 20 per cent.

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Transient Recovery-Voltage Characteristics of Electric-Power Systems

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1. Introduction

THE existence of transient recovery-voltage phenomena in connection with the interruption of short circuits by circuit breakers has been recognized for a number of years, and from time to time measurements and calculations have been made on actual field circuits.[2,5] However, while these more or less isolated and scattered instances have provided some valuable information, they have not afforded a comprehensive picture of transient-recovery voltage conditions existing in the field.

In order to provide the electric-power industry with a better understanding of and more complete data on transient recovery-voltage conditions, and, perhaps more particularly, to provide such data as a guide to the circuit-breaker designers, a field survey of several representative electric-power systems was recently carried out under the sponsorship of the committee on electric switching and switchgear of the Association of Edison Illuminating Companies. By permission of that organization and the participating electric power companies, some of the outstanding results and conclusions from this survey were made available to the AIEE committee on protective devices for presentation in this paper.

2. Transient Recovery Voltage—Theory and Definitions

While a number of excellent papers covering this phenomenon have been published in the literature, [1–6] a brief review is believed to be in order here.

The term recovery voltage applied to a circuit breaker refers to the voltage established across the open contacts after the arc is extinguished. The initial or transient recovery voltage is produced by a sudden change in circuit conditions, and its characteristics are determined by circuit constants. The normal-frequency recovery voltage is the generated system voltage finally maintained across the open contacts after the transient voltages have disappeared.

Since short-circuit currents are generally limited principally by reactance, it follows that the current is nearly 90 degrees out of phase with the voltage. Consequently, at the point of arc interruption, normally on or near the zero point of the current wave, the generated voltage is close to its maximum peak value. The voltage at the breaker, of course, prior to interruption, is held down to zero or nearly zero by the short circuit, but upon arc interruption attempts to recover immediately to the peak value of the wave. It is prevented from doing so instantaneously, however, by the effective capacitance across the breaker contacts, and there results instead a transient recovery-voltage oscillation. The frequency of this oscillation is determined by the inductance and capacitance of the circuit; the amplitude may reach double the normal-frequency crest voltage. In general, the combination of lumped reactance in the circuit between breaker and power source, with low capacitance-to-ground in the circuit between breaker and reactance, gives rise to the highest oscillating frequencies.

The severity of a given circuit is commonly characterized by the *rate of rise of transient recovery voltage*, which is determined by drawing a line from the zero point of the oscillating voltage wave, either to the peak of the first loop of the transient voltage oscillation, or as a tangent to the first voltage loop. This simple procedure, however, is not always adequate. The circuits involved are generally somewhat complex, so that the *transient recovery-voltage characteristics* are usually compounded of two or more superimposed oscillating frequencies. If, as occasionally happens, the first high-frequency loop has a smaller amplitude than a succeeding lower-frequency crest, the significance of rate of rise based upon the higher frequency but lower amplitude oscillation, may be decidedly questionable and subject to arbitrary judgment.

To avoid this difficulty, the complete transient recovery-voltage characteristic of a circuit should be determined. This characteristic, modified by circuit-breaker and test factors as described below, may be obtained directly from oscillograms taken in short-circuit tests, or it may be calculated from the constants of the circuit. In the AEIC survey of electric-power systems mentioned above, circuit characteristics were calculated in this manner, and the results were obtained in terms of an envelope of the transient recovery-voltage characteristics, composed of two and sometimes three major frequency components. In order to bring the task of calculating such characteristics within feasible limits in terms of man-hours of work required, certain approximations and simplifying assumptions were necessarily employed. A complete description of this simplified method of calculation, including the limits of accuracy obtained, is given in a companion paper.[7]

3. Results of Survey of Power-System Transient Recovery-Voltage Characteristics

The survey, which was made entirely by calculations except for direct measurements of a few circuit capacitances, was carried out on the systems of six power companies, selected as representing both concentrated urban types and distributed types of systems. Practically all of the circuit-breaker locations on these six systems were covered either by actual calculations or by a comparative estimating procedure. Where the system connections varied somewhat with different operating setups, and also where the assumed location of the fault affected the severity of the recovery-voltage characteristics, the calculations were made on the basis of the conditions which would give the most severe characteristics. With these results on the six systems covered by the survey, it was felt that a sufficient cross section had been obtained to draw general conclusions as to the transient recovery-voltage conditions existing throughout the industry.

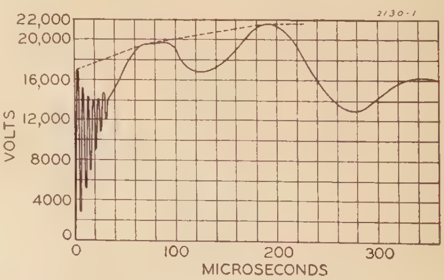

Figure 1. Typical recovery-voltage envelope and oscillating characteristic

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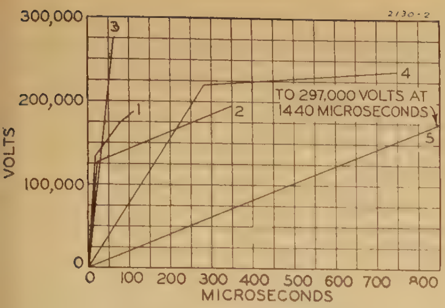


Figure 2. Representative envelopes of transient recovery voltage for 120,000- to 132,000-volt systems

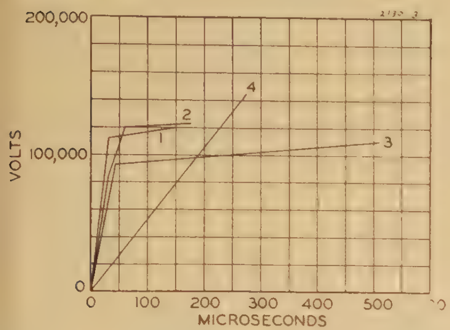


Figure 3. Representative envelopes of transient recovery voltage for 69,000-volt systems

As stated above, the survey was carried out on the basis of determining the envelope of the recovery-voltage characteristic, rather than on the basis of a simple rate of rise of transient recovery voltage. A typical recovery-voltage envelope, together with the complete oscillating characteristic from which it was drawn, is shown in Figure 1. Also groups of typical recovery-voltage envelopes, shown in Figures 2 to 6 inclusive, were chosen as illustrations from each of the following voltage classifications:

120,000- to 132,000-volt systems
69,000-volt systems
22,000- to 34,500-volt systems
11,000- to 13,800-volt systems
4,000- to 4,800-volt systems

For practical purposes, the envelope is sufficient to define the transient recovery-voltage characteristic, since it gives the magnitude of the first peak and the maximum peak, as well as any intermediate peaks found to be significant. Only the first peak and the maximum peak were recorded when the intermediate peaks were found to be less in amplitude than a corresponding point in time on the envelope connecting the first peak and the maximum peak. In other words, an intermediate peak was included if it raised the envelope, but not if it caused the envelope to drop below a straight line drawn between the first and maximum peaks. Also the envelopes were not extended beyond the point of maximum peak.

An analysis of the recovery-voltage curves shown in Figure 2 indicates a wide range of rates of rise for the various curves. Based on the first peak, curve 1 shows a rate of 8,300 volts per microsecond, while curve 5 shows only 210 volts per microsecond. The higher rates shown in curves 1 to 3 were obtained in connection with breaker locations where the fault was limited by a transformer fairly near the breaker, whereas the lower rates of curves 4 and 5 resulted from locations where there was no large lumped reactance in the circuit close to the breaker. Further examination of curve 1 shows that the first peak, which is 71 per cent of the final peak, occurs in 16 microseconds, the second peak in 80 microseconds, while the final and maximum peak is reached at 110 microseconds. Curves 2 and 4 show only

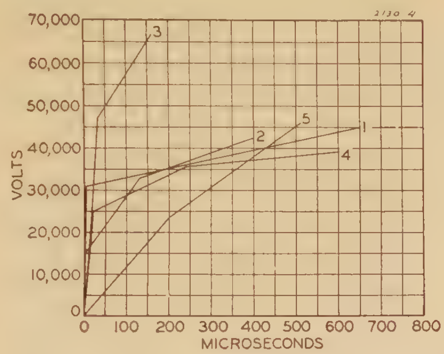


Figure 4. Representative envelopes of transient recovery voltage for 22,000- to 34,500-volt systems

two peaks, while curves 3 and 5 reach maximum values without any significant intermediate peaks. Figures 3, 4, and 6 in like manner show various typical characteristics for their respective voltage classes.

The envelopes shown in Figure 5 have been drawn in two ways:

1. With a scale going to 1,000 microseconds in order to include the complete characteristic.
2. With a scale of only ten microseconds in order to show the relatively high-speed transients in greater detail.

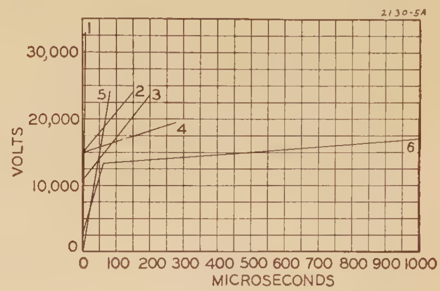
Complete numerical results of the survey were tabulated in terms of the information given on the envelope, that is, in terms of two or three significant peaks, and the time in microseconds required to reach each of these peaks. While these tabulations are not reproduced in this paper, the net results of the survey, in terms of rate of rise of transient recovery voltage, are given graphically in the form of cumulative percentage curves, shown in Figures 7 to 11 inclusive. The rate of rise used in these curves is based upon the first peak of the recovery characteristic, except in cases where this peak was found to be of abnormally low magnitude compared with later peaks.

The abscissae of these curves show the percentage of breaker locations where the rate of rise exceeds the value given by the ordinate. For example, referring to Figure 7, only 15 per cent of the breaker locations may be subjected to rates of rise of 5,000 volts per microsecond or higher. Also, it will be noted that 53 per cent of the breakers are located where the rate of rise does not exceed 1,000 volts per microsecond; the remainder of the breaker locations, or 32 per cent being subjected to rates of rise between 1,000 and 5,000 volts per microsecond.

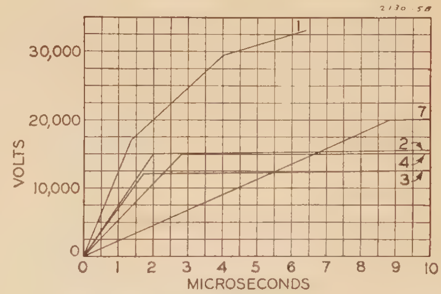
All of these results, of course, are given in terms of the characteristic of the circuits themselves and do not take into account the various factors which tend to modify the severity of the recovery-voltage characteristics actually obtained on a breaker. These factors include:

(*a*). Asymmetry of the current wave.

(*b*). Decay of flux in generators during short circuit.



5A—Envelopes to maximum peak



5B—Envelopes for first ten microseconds only

Figure 5. Representative envelopes of transient recovery voltage for 11,000- to 13,800-volt systems

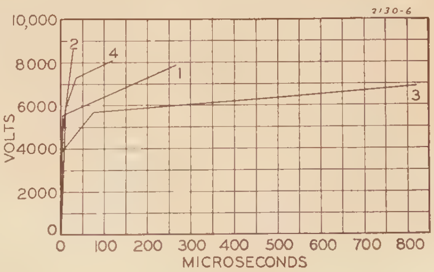


Figure 6. Representative envelopes of transient recovery voltage for 4,000- to 4,800-volt systems

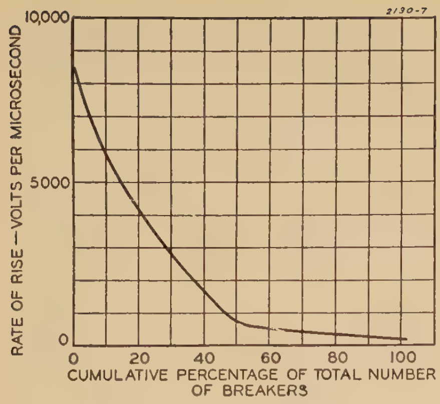


Figure 7. Cumulative percentage curves of rates of rise of recovery voltage for 120,000- to 132,000-volt systems

(Based on 345 breakers)

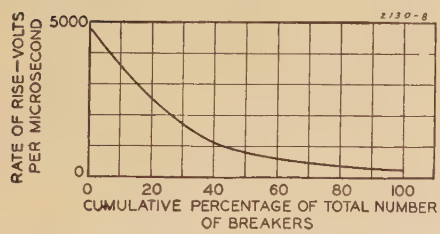


Figure 8. Cumulative percentage curves of rates of rise of recovery voltage for 69,000-volt systems

(Based on 342 breakers)

(*c*). Arc-voltage drop in the fault.

(*d*). Arc-voltage drop in the breaker.

(*e*). Conduction of current in the breaker after current zero.

In general, the above factors tend to reduce the severity of the recovery-voltage characteristic. On an actual circuit-breaker test, it is usually possible to account for the effect of these various factors by analyzing oscillograms of recovery voltage taken on the test and thereby to obtain a check on the calculated values.

The results of the survey, as summarized in Figures 7 to 11 inclusive, have shown that some of the calculated circuit recovery-voltage rates are substantially higher than the maximum rates disclosed by previous scattered information on this subject. Also the results show that the occurrence of high values of circuit recovery-voltage rates (on the order of 5,000 to 8,000 volts per microsecond) is considerably more widespread than had previously been suspected.

4. Significance of Results and Conclusions

In appraising the significance of these results, it is necessary first of all to take into account the modification of the maximum rate of rise of recovery voltage which is brought about in actual circuit-breaker operation. The data presented in a companion paper indicate the possibility that, for certain types of breakers at least, the effect of the breaker in reducing the severity of these transients increases as the calculated circuit values increase. Therefore, this may limit the actual rate of rise which a circuit can impress across the contacts of a breaker, regardless of the constants of the circuit. If this effect is rather general for breakers now in service, it may constitute one of the reasons why few outstanding instances of extreme rates of rise causing difficulty in the operation of circuit breakers have been recorded.

As to the significance of these results to circuit-breaker users, particularly in the operation of the great number of breakers already in service, and in view of the greater prevalence of fairly high rates of rise of recovery voltage than previously suspected, it may be said that there have been some but only a few instances recorded where circuit breakers were subjected to greater distress while interrupting short circuits under high rates of rise than under low rate conditions. In the group of systems covered by this survey, a study of operating records failed to disclose a systematic correlation between recovery rates and breaker distress or failures. Such effects as undoubtedly do exist would be evidenced in increased arc lengths and some additional maintenance due to increased burning and other phenomena. Experience seems to show in general, however, that circuit breakers have been designed with sufficient stroke to take care of even the highest rates of rise of recovery voltage, so that cases of breaker trouble directly attributable to recovery rates have been very rare.

On the other hand, it is believed that the results of the survey will be of major significance and importance in connection with the design and manufacture of circuit breakers. Important elements in the design, such as length of stroke and similar considerations, are determined to a large extent by rate of rise of recovery voltage. Therefore, while it may fortunately be true that liberal design allowances, combined with circuit-breaker modifying effects, have in the past produced breakers capable of withstanding the severe transient recovery-voltage conditions that are now known to exist, it may be more important in the future, particularly in connection with the development of radically new designs of circuit breakers, to have available a true picture of recovery-voltage requirements. It is believed that the results of the survey

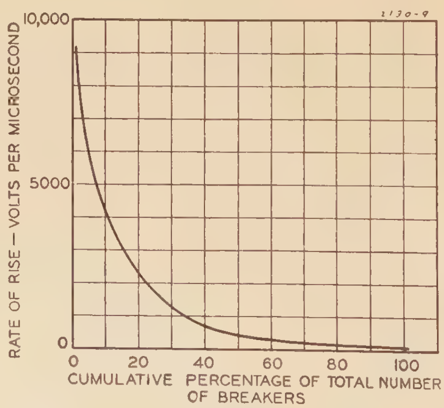


Figure 9. Cumulative percentage curves of rates of rise of recovery voltage for 22,000- to 34,500-volt systems

(Based on 1,975 breakers)

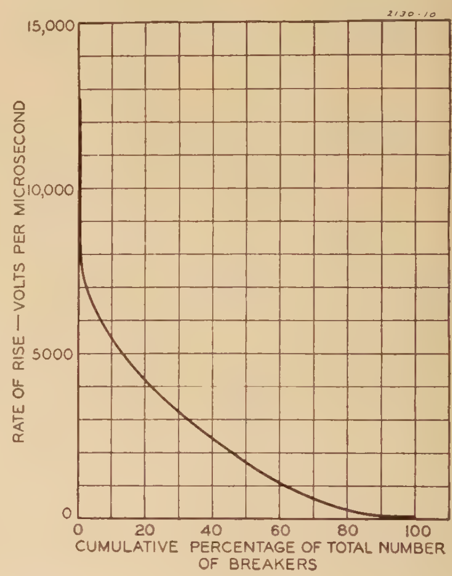


Figure 10. Cumulative percentage curves of rates of rise of recovery voltage for 11,000- to 13,800-volt systems

(Based on 2,250 breakers)

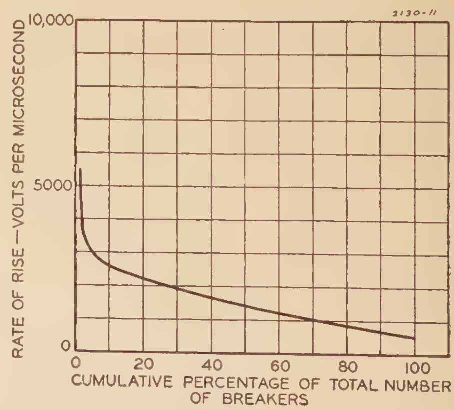


Figure 11. Cumulative percentage curves of rates of rise of recovery voltage for 4,000- to 4,800-volt systems

(Based on 3,975 breakers)

Emergency Overloads for Oil-Insulated Transformers

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IN periods of high industrial activity transformer loads are generally high, and it is essential that operators know what overloads they can carry safely in emergencies. In order to do this effectively, it is necessary to know what temperatures will be reached after various overloads for different durations of time and the effects of temperature and time on the dielectric and mechanical strength of the insulation. If limitations between these things and the degree of insulation deterioration can be established, it is possible to make recommendations for safe overload times and temperatures.

In Part I of this paper some new data are presented on the effect of temperatures and oil acidity on the insulation, and some recommendations for maximum temperatures and their duration are made. This paper differs from previous studies on this subject in that definite evaluation of the effect of acidity on the insulation strength is given. Acids are formed in service by contact between oxygen and transformer oil. Excluding this contact by blanketing the transformer with inert gas results in a high mechanical strength of the insulation at a given temperature and time, or, conversely, permits a high operating temperature for a given loss in life. In considering this paper, it should be borne in mind that the data refer to the mechanical strength of the insulation only. Neither the 60-cycle nor the impulse dielectric strength are affected to any extent by the conditions of test. The true criterion of insulation deterioration therefore is not mechanical strength alone, but the dielectric strength must be given due consideration. Experience has shown that satisfactory operation can be obtained with much reduced mechanical strength.

Even after the recommendations for temperatures and time duration for insulation are made, it is necessary to know the overloads and times which will give corresponding temperatures. Various sizes and types of transformers vary widely in this respect. Some transformers which are well ventilated may have low gradients and correspondingly high overload capacities; most of the larger power transformers come within this class. Smaller transformers are wound with small wire with many turns in a single coil. They will normally have much higher gradients between copper and oil and correspondingly lower overload capacities. In Part II the characteristics of these various types of transformers are given along with recommendations for safe emergency overloads for given time and temperature limits.

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Part I. Effect of Temperature on Cellulose Insulation

The life of cellulose insulation is decidedly influenced by three factors, namely, temperature, contact with oxygen, and exposure to acidity. Doctor C. F. Hill[1] has shown that cellulose insulation is stable for long periods of time below 90 to 95 degrees centigrade, when protected by an inert atmosphere. Doctor F. M. Clark[2] has classified the mechanical deterioration of cellulose as being caused by oxidation, pyrochemical changes, or both.

The object of this paper is to give additional evidence in regard to the rate of deterioration of cellulose insulation and to show that transformers can be exposed to short-time overloads even at high temperatures without damaging the mechanical strength of the insulation to a great extent.

Figure 1 shows the decrease in tensile strength of Manila paper when sealed at atmospheric pressure in an oxygen-free atmosphere. It is interesting to note the rapid decrease in tensile strength which takes place at 135 degrees centigrade. This occurs when temperatures are maintained at which thermal decomposition takes place. At 120 degrees centigrade, the tensile strength drops to approximately 50 per cent and levels off. It would appear that this 50 per cent strength would be maintained for long periods of time. Figure 2 shows the change in acidity of oil with Manila paper in oxygen-free oil protected by a nitrogen atmosphere. One of the main products of thermal decomposition is the formation of acid which forms a catalyst for further decomposition. The inert atmosphere prevents the formation of acid product at 120 degrees centigrade and below, showing the decided advantage of protecting the oil of a transformer against exposure to air.

The same data for Figure 1 are used to produce Figure 3. The remaining tensile strength of Manila paper after exposure to different temperatures in oxygen-free oil for different periods of time is plotted in this figure with temperature and time as ordinate + abscissae. These curves are of special interest to both the designer and the operator of transformers, because they can be used to predict the amount of mechanical damage done to transformer insulation after it has been operated at given temperatures for specified lengths of time. For example, if not less than 50 per cent mechanical strength is required at all times, it is interesting to see that the total period of operation at 140 degrees centigrade should be not

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will have their greatest value in providing this information.

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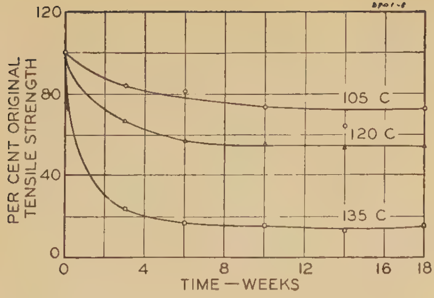

Figure 1. Decrease in tensile strength of Manila paper in oxygen-free oil protected by a nitrogen atmosphere

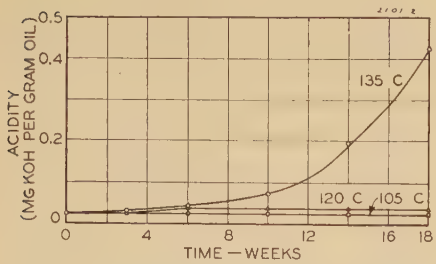

Figure 2. Change in acidity of oil with Manila paper in oxygen-free oil protected by a nitrogen atmosphere

more than two days, because in this time the *cellulose* has decreased in tensile strength to 50 per cent. However, with overloads at which the temperature reached only 135 degrees centigrade, a total of five days would be possible. If 40 per cent of the original mechanical strength is the lowest desired, a total of nine days at a temperature of 135 degrees centigrade may be allowed.

Figure 4 is a plot of the lowest tensile strength for paper insulation after 18 weeks under oxygen-free oil at various temperatures. From this figure one can select the temperature for transformer operation when the ultimate tensile strength has been selected. For example, if not less than 60 per cent of the original strength is required, the operating temperature should never be more than 115 degrees centigrade.

Based on the above data, the following practical conclusion might be drawn. The load which may be carried in the operation of a transformer is governed by the desired life of the transformer. From the experimental data presented here it is felt that an oil-insulated transformer may be operated at temperatures up to 120 degrees centigrade continuously, and even above, during short-time overloads, without seriously damaging the insulating materials, provided an inert (oxygen-free) atmosphere is maintained in contact with the oil at all times, and provided the acidity of the oil in contact with the insulating materials is maintained at a low value. Below 120 degrees centigrade the main deterioration in mechanical strength is caused by oxidation, while above this temperature the thermal decomposition of the cellulose is very detrimental. The thermal change results in the formation of gases, water, and other decomposition products. Oxidation, which takes place below 120 degrees centigrade, is eliminated to a very large extent by the use of an inert atmosphere.

Figure 5 shows the decrease of tensile strength of Fuller board when included with a varnished cotton tape in oxygen-free oil protected by a nitrogen atmosphere and at atmospheric pressure. It is quite interesting to compare this curve with Figure 1. At 135 degrees centigrade, the rate of decrease in tensile strength is quite similar to that of paper with no acids present other than those from decomposition. The decided effect of the acidity can be seen at 120 and 105 degrees centigrade. At 120 degrees centigrade,

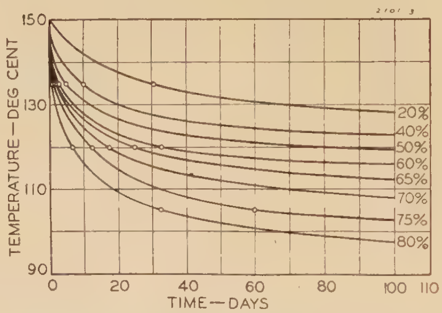

Figure 3. Percentage of initial tensile strength reached by Manila-paper insulation in oxygen-free oil protected by a nitrogen atmosphere

the final tensile strength is less than twenty per cent as compared with 54 per cent with no acids present; and for 105 degrees centigrade, 44 per cent as compared with 72 per cent. It would appear from these comparisons that it is of utmost importance to maintain low acidity and to avoid acid-forming materials in the construction.

The acidity in contact with the Fuller board samples from the varnished cloth is shown in Figure 6. While these acidities are higher than those of usable transformer oil, it simulates the effect of lower acidities over longer periods of time.

The data for Figure 3 are used again to produce Figure 7. The curves show the part of the initial tensile strength remaining at the end of various periods versus temperature. If we choose 50 per cent as the minimum mechanical strength required, this would be reached in 1.5 days compared with two days from Figure 3. Likewise, at 135 degrees centigrade, the insulation would have reached 50 per cent strength in three days. For 40 per cent strength the total time of overloads could be seven days, compared with nine days with low acidity. A significant difference between this figure and Figure 3 is that the tensile strength continues to decrease even after 100 days, while in the low-acidity oil the decrease in tensile strength shows a greater tendency to reach a constant value for a given temperature.

Figure 8 shows the lowest tensile strength for Fuller board insulation after 18 weeks of exposure to high-acidity oil. If we choose 50 per cent as the minimum required tensile strength, the operating temperature should not be more than 100 degrees centigrade. However, this is for a period of only 18 weeks and since the tensile strength continues to decrease, due to the presence of acids, it would indicate a still lower temperature.

The curves of Figures 5 and 8 show that the presence of acids in contact with cellulose insulation seriously catalyzes not only oxidation changes but also thermal decomposition. Care should be taken to maintain low-acidity oil in order to obtain the longest life from the cellulose insulation. Varnishes and varnished tapes have been a source of acids in transformers. The use of synthetic materials in place of natural varnishes in modern transformer construction has practically eliminated this source of acids.

Figure 9 shows the similarity in behavior between Fuller board and paper insulation. The Fuller board was in contact with oxygen-free oil under the same conditions as for Figure 1. While the Fuller board is some better at 105 and 120 degrees centigrade, it is interesting to note that at 135 degrees, thermal decomposition destroys most of the tensile strength.

Figure 10 shows the rate of increase in acidity of oil samples with the same materials as for Figure 6, except that the oil has free access to the oxygen of the air. Here the acidity shows a decided increase in 18 weeks, much greater than in Figure 2. Comparison of these figures shows that to obtain full advantage of preserva-

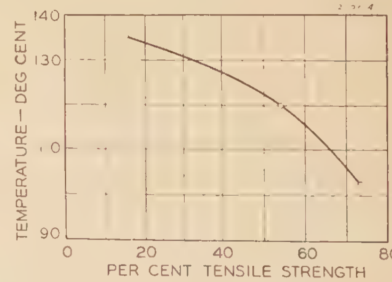

Figure 4. Lowest tensile strength for paper insulation after 18 weeks under oxygen-free oil protected by a nitrogen atmosphere

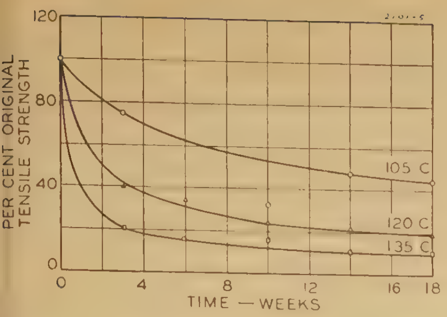

Figure 5. Decrease in tensile strength of Fuller board, together with varnished cloth, in oxygen-free oil protected by a nitrogen atmosphere

tive measures in transformers, no acid-forming varnishes or treatments should be used.

Figure 11 shows test data taken on paper in transformer oil at various temperatures with the samples open to the air. Comparison of these curves with Figure 1 shows the great benefit obtained by protecting the samples from oxygen, particularly at temperatures of 120 degrees centigrade and less.

It is possible from the data given above to make recommendations for short-time emergency overload temperatures of transformers based on given amounts of decrease in mechanical strength of the insulation. One set of values that has been proposed is given in Table A.

Inspection of these values will show that if a given temperature from the table were reached 20 times for the time specified, the insulation would have a tensile strength of approximately 80 per cent of its original value. If emergency overloads of the same character as described in the table were to be applied once a year for 20 years, we would, therefore, not expect that the insulation would be appreciably lower than 80 per cent of its original value. Figure 3 is used in reaching this conclusion. This assumes that there was no deterioration due to steady operation. It would be more practical, however, to consider that the transformer

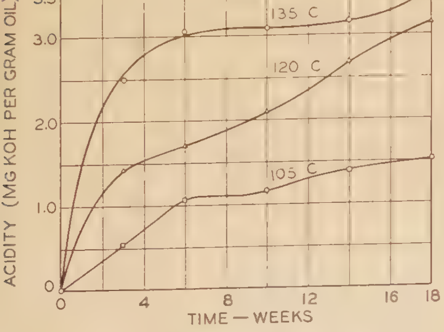

Figure 6. Change in acidity with Fuller board and varnished cloth under oxygen-free oil protected by a nitrogen atmosphere

might reach 105 degrees hot-spot temperature for considerable durations of time, and, if this were true, its mechanical strength would be somewhere between 80 and 75 per cent of its original value. If the emergency operations were to take place after this initial deterioration, a method of estimating its ultimate deterioration is as follows:

Suppose we were to consider that there would be 20 overloads for 24 hours which would reach 115 degrees; then the deterioration due to the steady load would be equivalent to approximately 24 days at 115 degrees without the deterioration due to the overload. The additional 20 days would then be equivalent to 44 total days at 115 degrees, which would result in approximately 65 per cent of the original mechanical strength of the insulation. Similar methods of calculation could be used for any of the other emergency overload temperatures. If an operator desired to determine the amount of deterioration under other conditions of loading and time, the curve data provide an easy graphical method for making such an estimate.

It is believed that the decrease in mechanical strength resulting from Table A is reasonable and safe for general use. The ultimate strength requirements depend upon many factors determined by service conditions. The severity and frequency of short circuits is an important factor, as well as whether or not the apparatus remains fixed or is to be transported. Since the dielectric strength remains practically unchanged, a reduction of 40 to 50 per cent in the mechanical strength of the insulation over a 20-year period would not be considered excessive for normal operating requirements.

Summary

New data on the rate of deterioration of cellulose insulation at different temperatures have been obtained. From them a table of time periods and temperatures has been made at which oil-insulated transformers may be operated in order that specific cellulose insulation strengths of approximately 65 per cent of the original value may be maintained. It is expected that this value will be maintained even after operation of a considerable length of time at 105 degrees centigrade.

Table A. Duration of Overload Ultimate Temperatures

Hours	Degrees
1/4	145
1/2	140
1	135
2	130
4	125
8	120
24	115

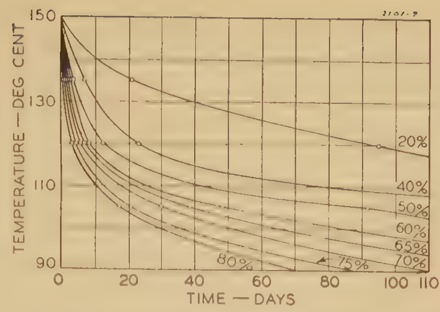

Figure 7. Percentage of initial tensile strength reached by Fuller board, together with varnished cloth, in oxygen-free oil protected by a nitrogen atmosphere

Part II. Recommended Emergency Overloads for Transformers

After Table A in Part I has been established, it is still necessary to correlate the data with overloads which will result in these temperatures after given lengths of time. In order to do this, it is necessary to know what the characteristics of average transformers are, both in respect to the gradient between the insulation and the oil, and the length of time which it takes for the oil to reach given temperatures under given load conditions. Data may be found in several places for estimating the gradients between oil and insulation and also the rate of oil rise during transformer overloads. One such reference is a recent paper on "Hot-Spot Temperatures in Transformers."[3] Before these data can be used, however, it is desirable to establish available characteristics for several sizes of transformers.

It is suggested that seven classes of

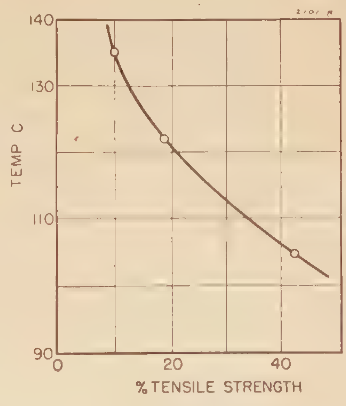

Figure 8. Lowest tensile strength of Fuller board, together with varnished cloth, after 18 weeks in oxygen-free oil protected by a nitrogen atmosphere

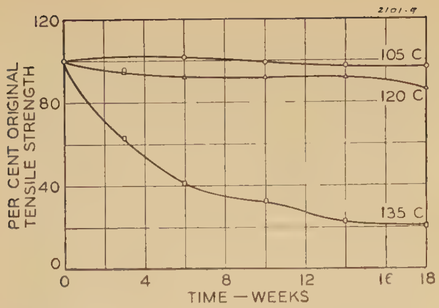

Figure 9. Decrease in tensile strength of Fuller board in oxygen-free oil, protected by a nitrogen atmosphere

transformers be considered. These classes may be described as follows:

Class 1. Many transformers use round wire, and there is a considerable gradient between the center of such coils and the oil. There are also some ribbon-wound coils, which have several layers and several turns per layer. We suggest that this class include transformers up to 150 kva, 25 kv, and transformers with windings of all voltage classes rated at 15 amperes or less. This class is to be oil-insulated, self-cooled.

Class 2. The next larger transformers than those described in class 1 generally have strap windings with at least one side or edge of each conductor exposed to oil. Surely, up to 69 kv such transformers would not have heavy conductor insulation; therefore, this class is limited to power and distribution transformers up to 69 kv, oil-insulated, self-cooled.

Class 3. This class is similar to class 2 except that it is for forced air-cooled transformers.

Class 4. Some transformers include regulating equipment and are used as unit substations. Such transformers are very similar to the ordinary self-cooled transformer with the exception that the tank is larger, and there is much more oil used than in the ordinary self-cooled transformer. The result is that the transformer has a longer time constant than the ordinary self-cooled transformer. It is suggested, therefore, that class 4 include transformers such as unit substation transformers and be similar to class 2 except for longer time constants.

Class 5. It is suggested that this class include oil-insulated unit substation transformers, which are cooled with forced air. This class is then the same as class 3, except that the transformers have longer time constants.

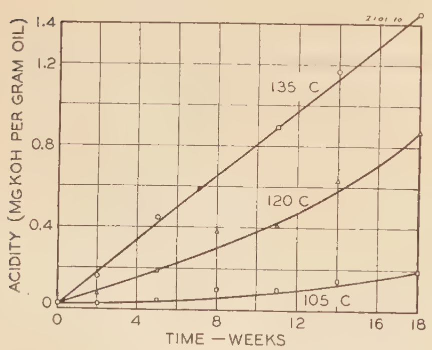

Figure 10. Change in acidity with Fuller board under oil exposed to air

Table I. Suggested Classes for Transformers for Calculating Emergency Overloads

	Class 1	2	3	4	5	6	7
Hot-spot rise	60°	60°	63°	60°	63°	62°	65°
Top-oil rise	40°	50°	47°	50°	47°	50°	46°
Loss ratio	2.5:1	2.5:1	4.5:1	2.5:1	4.5:1	2.5:1	4.5:1
Time constant	3	4	2.5	6	4	4	2.5

Class 6. Large high-voltage power transformers may require heavier insulation on the conductors than smaller transformers. This, in turn, will result in higher gradients between the copper and the oil. Therefore, slightly different characteristics should be used for these transformers than for smaller units exemplified in class 2; class 6 should be approximately the same as class 2 except for higher gradients.

Class 7. It is suggested that class 7 correspond to class 6 except that the transformers have forced air-cooled ratings.

A tabulation of the proposed characteristics of these various classes is given in Table I, and it might be well to describe how the various values were obtained. For example, in class 1 the designer can calculate the watts per square inch of losses transmitted from the coil surface to the oil. From this, he can make an approximate estimate of the gradient between the coil and the oil, based on designs in common use. For class 1 this gradient should be in the order of and not exceeding 20 degrees centigrade at normal load. Likewise, the top oil rise is in the order of 40 degrees for such transformers. The difference between average oil and top oil is in the order of five degrees. The loss ratio refers to the ratio of the copper loss to the iron loss at rated load, and this loss ratio approaches limits now commonly used. Time constants are given in hours. These values are average values for transformers of the ratings referred to above.

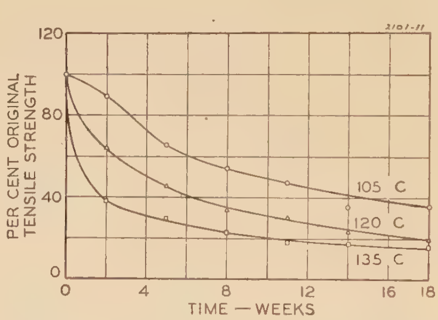

Figure 11. Decrease in tensile strength of Manila paper under oil exposed to air

In a previous paper[3] a method was indicated for calculating transformer hot-spot temperatures. In this paper it was shown that changes in viscosity tend to decrease gradients at high oil temperatures. These decreases further tend to counteract increases in gradients due to losses. Therefore, no great error should result if the change in copper resistance were ignored. This is not exactly true but probably is within reasonable error, considering that average characteristics of transformers are being considered. On this basis, it would be reasonable to assume that the gradient between the copper and oil varies approximately as the 0.8 power of the copper losses, which, in turn, are assumed to vary as the square of the load.

In the paper[3] previously mentioned, a method was shown for calculating oil temperatures after short-time loads. Cal-

Table II. Emergency Short-Time Overloads (Per Cent of Rated Load)

Time (Hr.)	Temperature	Class 1	2	3	4	5	6	7
		Following No Load						
1/4	145	253	360	291	372	288	333	252
1/2	140	230	320	248	334	259	289	229
1	135	205	265	210	295	230	249	196
2	130	180	215	172	239	188	202	166
4	125	155	170	145	193	157	167	143
8	120	138	145	128	158	135	141	127
24	115	130	130	123	132	124	128	121
		Following Full Load						
1/4	145	215	280	225	296	230	271	206
1/2	140	197	255	205	265	208	238	187
1	135	180	218	183	234	185	205	166
2	130	163	185	155	198	160	174	149
4	125	148	155	140	169	140	151	135
8	120	135	138	128	147	131	138	126
24	115	130	130	123	132	124	128	121

culations were made on the basis of average oil temperatures, and an approximate constant value added to them. The method in common use before was to calculate the top oil temperatures directly. Comparative calculations show that there is no great difference between the two methods of calculation. For the purpose of simplicity and also for greater conservatism, it is intended to use the older and simpler method in this paper.

For example, suppose we take class 1 and calculate the overload, which can be carried for eight hours to reach a temperature of 120 degrees centigrade, at an ambient temperature of 30, following full load. As shown in the preceding paper, it is easiest to use "cut and try" methods, and a value of 135 per cent load can be assumed as a basis. At 135 per cent load the copper losses will be 1.82 times as great as at full load. If the total losses at full load in the first case were 3.5 times the iron loss, in the second case it would be 5.55 times the iron loss. If the top oil rise were 40 degrees in the first case, it would be ultimately, $\frac{(5.55)^{0.8}}{(3.5)} \times 40$ degrees or approximately 58 degrees centigrade. The change in load from full load to 135 per cent results in an ultimate change in oil temperature of 18 degrees. This change will not be entirely completed in eight hours, and the rise which will result in that time will be:

T_8 hr $= 18(1 - e^{-8/3})$ = approximately 17 degrees

This means that the oil rise after eight hours will be 57 degrees.

The gradient at full load is given as 20 degrees. At 135 per cent load the copper loss is 1.82 times the value at full load, and the gradient should be $1.82^{0.8}$ or 1.6 times as great as the gradient at full load. 1.6×20 degrees is 32 degrees. The total temperature would be the sum of the ambient temperature, 30 degrees, plus the gradient between air and top oil, 57 degrees, plus the gradient between oil and copper, 32 degrees, or a total of 119 degrees centigrade. Since this temperature is slightly below the 120 degrees allowed, this percentage was considered sufficiently accurate for use in Table II.

It will be seen upon inspection of Table II that the proposed overloads are much higher than those previously recommended for use in the American Standards Association Guides for Operation for Transformers. This occurs, partly because the original guides did not differentiate between different types of transformers, and partly because temperature rises which were permitted were lower than those proposed in Part I of this paper. Still another reason is that the number of emergencies was not specifically limited, and in the present paper 20 are assumed, on the basis of 20-year life and one emergency per year on the average. It is felt that sufficient information is given in this paper to permit the operator to use judgment and discretion in specifying safe overloads for his application.

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A New Jewel for Indicating Instruments

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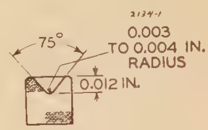
It has been almost universal practice to use highly polished sapphire jewel bearings, cut approximately in the form shown by Figure 1a, in electric indicating instruments. Such a jewel is commonly known as a "Vee." The other member of the bearing is a cone-shaped piece of hardened steel or other hard metal also accurately cut and highly polished. Figures 2 and 3 show both the common proportions of these parts and the clearances involved. Figure 2 shows an instrument which has the moving shaft vertical, and Figure 3 an instrument with the moving shaft horizontal. Figure 4a shows typical moving systems and jewel settings.

The making of sapphire Vees is a very highly specialized art, and the great majority of machine equipment and trained personnel has been concentrated in a small area in Europe, the principal production being in Switzerland.

The present conflict has cut off the source of supply of instrument bearings and has left this country with inadequate facilities for their production. Accordingly, it has been necessary to find substitutes. Furthermore, because of the vital part that instruments play in devices for the armed forces and industry, it has been necessary to find a substitute which would not in any way impair the usefulness of the instruments even under abnormally severe conditions of use.

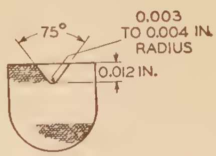
Fortunately, instrument manufacturers have a good deal of experience to fall back on. About 30 years ago, the present so-called "miniature instruments" (Figure 4) were introduced, and the demand for them has steadily increased, particularly in the communication industry. Because of the light weights of the moving systems and proportionately lower torques available, the radius of the pivot used was considerably smaller than that used in larger instruments. This created a demand for a sapphire jewel with a correspondingly smaller radius of the spherical surface in the bottom of the Vee.

In general, the problem of producing a sapphire jewel of the required shape resolves itself into that of cutting very accurately to shape a piece of sapphire, which is in a group of materials next in hardness to diamonds. The reason that the shape is particularly difficult is that the apex of the Vee must be accurately spherical, with a radius of from 0.002 to 0.004 inch, and the side tangent to the spherical surface must be straight. At the center of the spherical surface if, for example, the cut in the sapphire is made in a lathe, the linear cutting speed is zero and even at a speed of, say, 30,000 rpm at a distance of 0.001 inch from the center, the cutting speed is only 0.3 foot per sec-



(a)

Figure 1a. Cross section of a sapphire jewel used for miniature instruments



(b)

Figure 1b. Cross section of a hot-formed jewel as described in this paper

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F. K. McCune is assistant to the executive engineer, and J. H. Goss is engineer in the works laboratory, both with General Electric Company, West Lynn, Mass.

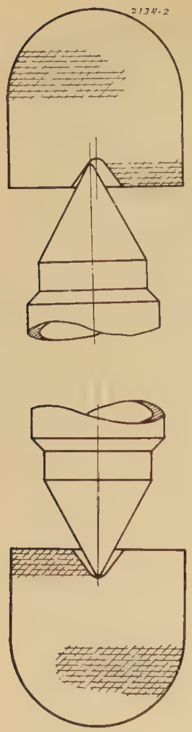

Figure 2. Scale drawing showing proportions and clearances of vertical shaft miniature instrument bearing

Figure 4 (above). A group of modern miniature instruments

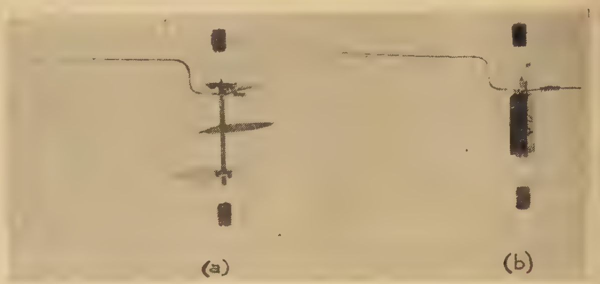

Figure 4a (right). Miniature instrument moving systems and jewel settings

(*a*). For a-c instruments
(*b*). For d-c instruments

ond. Also, as can be seen from the dimensions (Figure 1a), the cutting tool must be small in size. Therefore, the tendency in cutting, smoothing, or polishing is to produce a surface which is not truly spherical, but has either a lump in the center because of the low cutting speed, or a hollow which may be produced by crushing the center instead of cutting it.

As mentioned before, the small sapphire jewel for miniature instruments was necessary to limit the amount of side play in the jewel. As can be seen from Figures 2 and 3, the side play may amount to more than the end play. It is of importance both from the standpoint of its effect on mechanical clearances, and because excessive side play introduces uncertainty of pointer position. The latter is of most concern when the instrument may be used sometimes with shaft horizontal and at other times with shaft vertical. Referring to Figure 3, it is obvious that the only way to get a small degree of side play in a jewel of inherently large radius is to reduce the clearance between the pivots and the bottom of the jewels to a small fraction of a thousandth of an inch. This is an impractical solution, because of the hazard of very slight dimensional changes causing stickiness in the instrument, and because of the wedging action caused by the very small angle of slope of the jewel near the center.

The generally unsatisfactory contour in jewels available at the time led manufacturers to look for substitutes which could be produced by other methods. As a result of a large amount of development work done in the General Electric Company laboratories about 25 years ago, a method of producing a jewel by forming a drop of fused "hard glass" was developed. These jewels had very accurately controlled contours, since the glass could be made to almost any desired shape. The general shape is shown in Figure 1b. They also had very smooth highly polished surfaces. In respect to shape they were far superior to the sapphire then readily available. Such jewels had, however, two defects:

1. The manufacture had to be very carefully controlled and was not quite as simple as the description above may sound.
2. The jewel would show a microscopic indentation under an impact such as that produced by dropping the instrument, or by a heavy blow on the mounting panel.

This microscopic blemish in the surface would, however, in general not be noticeable in the performance of the instrument unless the pivot happened to run on the exact spot where the blemish had been made. Even in the latter case, the performance of the instrument would not be materially affected, unless a large number of pointer oscillations or the presence of vibration caused excessive wear at the roughened spot.

As has been mentioned before, a large number of these jewels were used, and their use probably served as an incentive to better the quality of sapphire jewels. In any event, after the hot-formed jewels had been used for several years, sapphire jewels became available with much improved contour, smaller radius of the spherical surface in the bottom of the Vee,

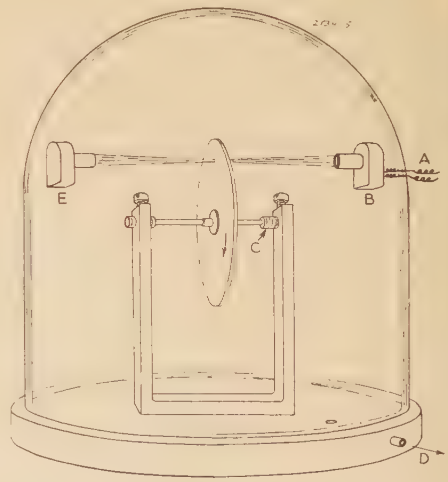

Figure 5. Apparatus for determining coefficient of friction

A—To amplifier and chronograph
B—Phototube
C—Vee jewel bearings or ring jewel bearings under test
D—To vacuum pump
E—Light source and lens

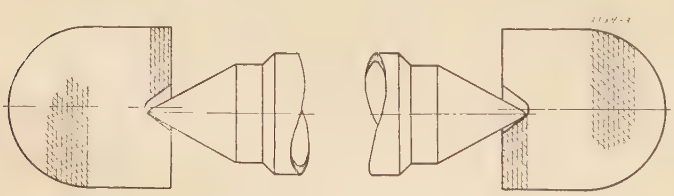

Figure 3. Scale drawing showing proportions and clearances of horizontal shaft miniature instrument bearing

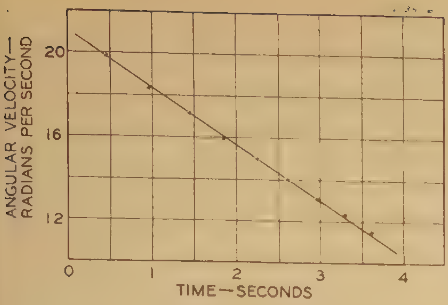

Figure 6. Velocity versus time curve

Deceleration 2.66 radians per second per second

and in every way suitable for instrument use. As a result, the use of the hot-formed jewel was practically abandoned although they have been used in certain instruments from time to time with satisfactory field performance.

The present emergency made necessary a review of the whole situation and the following steps were taken:

1. The performance of the hot-formed jewels and sapphire jewels was carefully re-evaluated.

2. A search for the optimum material for a hot-formed jewel was started, since it was recognized that new materials might be available which would be distinctly superior to that chosen as the best 10 or 15 years ago. The evaluation of relative performance of the hot-formed jewel and the sapphire jewel was carried out in the following manner with results as shown in the corresponding tables and figures:

(*a*). *Friction.* One criterion of instrument performance is that portion of the error in an instrument with a horizontal shaft caused solely by the friction between the pivot and jewel. This, when measured by noting the difference between upscale and downscale readings untapped in an instrument free from magnetic hysteresis, is known as jewel roll. Jewel roll, expressed in radians, is a function of moving system weight, restoring torque, radius of the pivot, and coefficient of friction between pivot and jewel. The formula has been given as follows: for the usual case where the end of the pivot is spherical and the surface of the jewel conical:[1]

$$\text{Jewel roll} \propto \frac{WCr}{K}$$

W—weight of the moving system in grams

C—coefficient of friction between the pivot and jewel

r—radius of the spherical end of the pivot in millimeters

K—restoring force in gram-millimeters per radian

In order to determine the friction between an instrument pivot and the corresponding jewel surface, two jewels of the material to be tested and a rotating member made up to approximately duplicate an instrument moving system were mounted in a vacuum (Figure 5). The moving system was made to rotate, the force causing this rotation was removed, and the deceleration measured. Figure 6 is a typical deceleration curve. From this deceleration, the moment of inertia, and dimensions of the parts involved, the coefficient of friction was calculated. While not having direct bearing on this investigation, certain data taken on ringstone jewels are included.

Table I. Measured Coefficient of Friction When Run Against a High-Carbon Steel Pivot

Material	Form	Coefficient of Friction
Sapphire	Ring	0.140
Leaded Brass	Ring	0.188
Plastic *A*	Ring	0.160
Plastic *B*	Ring	0.190
Graphite	Ring	0.160
Sapphire	Vee	Range 0.15 to 0.19
Glass	Vee	0.18
Material *H* (Figure 11)	Vee	0.18
Ceramic polished	Vee	0.28
Ceramic unpolished	Vee	0.33

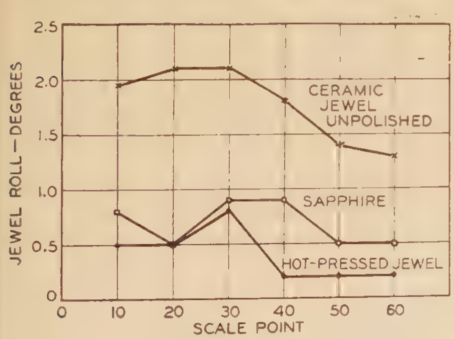

Figure 7. A comparison of jewel roll in a single instrument taken from three different sets of Vee jewels

(*b*). Actual measurements of jewel roll with three materials involved were made using a very low torque instrument, with the results shown in Figure 7. These measurements were, of course, not nearly as accurate as the measurements under (*a*), but were simply confirmation that the measurements as taken in (*a*) are directly applicable to the problem.

(*c*). An impact device was prepared, two views of which are shown in Figure 8. After some experimentation, a test schedule was worked out as follows: The pendulum was raised ten degrees from the vertical, allowed to fall and strike the panel, and caught on the rebound. The instrument jewel roll was measured and the process repeated, the angle of drop being increased each time by ten degrees until 180 degrees was reached. Figure 9 shows the performance of two instruments mounted as shown on the same panel, one with sapphire jewels and one with hot-formed jewels from material *G* (Figure 11), which was the material used for jewels in the past.

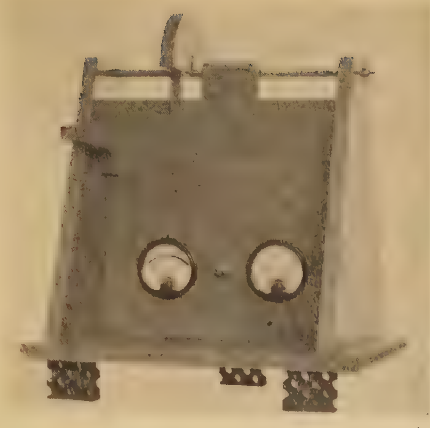

Figure 8a. Impact testing device—front view

Figure 8b. Impact testing device—back view

(*d*). After impact a group of instruments, half with sapphire and half with hot-formed jewels of material *G*, were run from zero to full scale and back approximately 900,000 times, and the jewel roll again noted with results as shown in Table II.

All of the above were done with several different instrument designs and torques, and representative values are given in the tables. Conclusions from the above were that it was highly desirable to increase the impact strength of the hot-formed jewel.

(*e*). An arrangement as shown in Figure 10 was made by which a jewel could be accurately located below the opening of a glass tube. A hardened steel instrument pivot was placed in the Vee and struck by dropping a pellet weighing 0.1 gram upon it. As shown in the figure, the pellet was raised by a magnet to an exact height and released by removing the magnet. After each drop, the jewel was carefully examined under the microscope for any traces of fracture of the surface and another trial made if no fracture occurred. The method finally worked out as giving the most consistent results was to

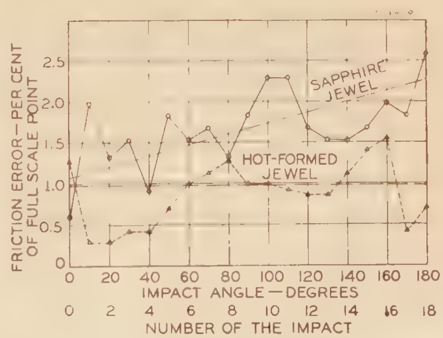

Figure 9. Increase of instrument friction as a result of successive impacts in ten-degree steps on panel impact device

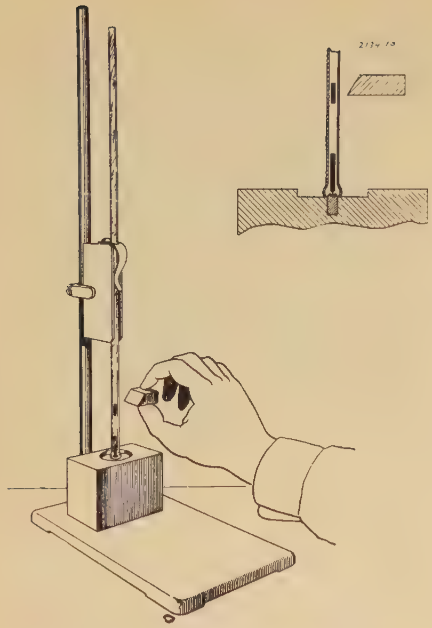

Figure 10. Jewel impacting device

drop the weight five times from a distance of 2 inches, five times from a distance of 6 inches, and five times from a distance of 12 inches. A jewel would be given a score of one for each time it successfully withstood a 2-inch drop, a score of three for each time it successfully withstood a 6-inch drop, and a score of six for each time it withstood a 12-inch drop. The total score of a group of five jewels was used in making the evaluation in each case. It should be noted that the pivots were initially selected by 100-diameter contour measurement and a 40-diameter visual inspection to be sure that they did not introduce another variable.

Figure 11 shows the comparative values of a number of different materials including material *G* which had been in use for many years.

It should be noted that the impact strength is not a function of material only, since jewels were actually produced from material *H* which varied in impact strength from practically zero when overstrained to a value of 30 when completely annealed as shown at *I*. The value plotted at *H* is the average which can be held for the optimum manufacturing process worked out.

The materials investigated covered ordinary soft glass *A*; a considerable

Table II. Jewel Roll of Instruments After Approximately 900,000 Pointer Movements Up and Down Scale

	Instrument No.	Angle* (in Degrees)
Hot-formed jewels	1	3.2
	2	3.2
	3	0.3
	4	1.5
	5	0.6
Sapphire jewels	1	1.0
	2	0.9
	3	1.0
	4	1.0
	5	1.2

Initial jewel roll was negligible.

* Instrument scale angle 90 degrees.

range of so-called "hard glasses," *B*, *C*, *D*, *E*, *F*, *G*, the material used some years ago, and *H*. Material *H* has a very high softening point and is unique in having a large percentage of alumina.

Since vibration is present in many instrument applications, *H* jewels were compared to sapphire in respect to bearing life under vibration. A table which gave each point in the instrument a circular motion in a plane 45 degrees from the horizontal with a diameter of 0.020 inch and a frequency of 1600 rpm was used. Results showed the two bearings to be comparable.

From an analysis of the above data and examination of the pivots and jewels it was obvious that material *H* represents very distinct progress over the previously used glass jewel. Since sapphire is harder than the steel pivot, the effect of heavy impact is to cause a deformation of the pivot. When this deformation occurs, the precision of the instrument is impaired, because the moving system is no longer supported by a smooth bearing, but is rolling on an irregularly shaped mushroomed pivot. This effect will always be present and will be noticeable at any part of the scale, since the whole end of the pivot is deformed. In the case of material *H*, the point at which the jewel material deforms is approximately the same as that at which the pivot deforms, so that up to this critical point, impact has no effect. Beyond this point, there is very

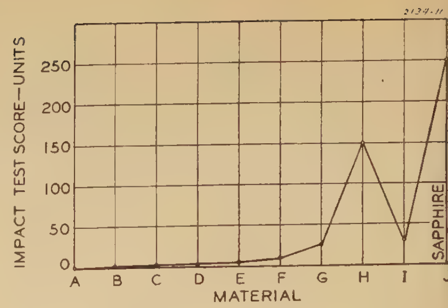

Figure 11. Impact tests on hot-formed jewels and on sapphire jewels

slight deformation of the pivots, and a minute area of the jewel surface is crushed. Because of the fact that the pivot flies up from its regular seat under impact, this deformed part of the jewel is very likely not to be at the point where the pivot usually travels and, therefore, is unlikely to affect the performance of the instrument in any way.

The technique of manufacturing instrument jewels with carefully controlled dimensions to give the impact strength of *H* has been worked out, and these can now be produced in quantities sufficient to assure that the jewel bearings will not be a limiting feature in the quantity of instruments which may be produced.

In conclusion, the original result desired was a jewel which could be produced in quantity, and which would involve the least possible sacrifice over sapphire for miniature instruments. The data indicate that there is little to choose between the new jewel and sapphire for actual miniature instrument application.

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A New Single-Phase-to-Ground Fault-Detecting Relay

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Synopsis: In the application of differential relays the need for a supervising relay which will detect the existence of a single-phase-to-ground fault condition to the exclusion of all others has frequently arisen. Heretofore, the only scheme available has been to utilize a relay energized by zero-sequence quantities. Generally, the relay has been energized by a current transformer connected in the station ground. Such a relay, however, will also detect the existence of a two-phase-to-ground fault and only partially solves the problem. The new relay described in this paper derives its operating force from the zero-sequence voltage at the bus and is restrained by the negative-sequence voltage at the bus. The addition of properly proportioned negative-sequence restraint provides the relay with a means of recognizing a single-phase-to-ground fault only. It is applicable on those systems where the zero-sequence impedance of the system exceeds the negative-sequence impedance by a reasonable margin.

Application

IN the application of differential relays to the protection of generating station busses, it is frequently the case that single-phase-to-ground fault protection only is desired. In such cases, the differential relay is connected in the residual circuit, or zero-sequence network, from the current transformers. Again, it is sometimes desirable to supplement three-phase differential relays with a fourth differential relay connected as above in order to obtain increased sensitivity for phase-to-ground faults. This latter condition is frequently encountered when systems are grounded through an impedance, so that the maximum value of phase-to-ground fault current is distinctly limited. In such cases the setting of the fourth or ground relay is considerably more sensitive than that of the phase relays, and the problem then becomes one of keeping the residual differential relay from tripping erroneously for heavy external phase-to-phase short circuits. Where conventional relays are used with current transformers, the major difficulty arises when a false differential current appears in the ground relay caused by unequal saturation of the various current transformers in the faulted phases, this condition being particularly emphasized by the presence of d-c transient. When the linear coupler relay scheme[1] is used, the fourth or ground relay set sensitive may also operate falsely for heavy external phase faults, not because of saturation, but depending upon the relative values of the maximum fault currents, the sensitivity of the relay, and the accuracy of the linear coupler transformers.

A solution to this problem is to block out the ground relay for every type of fault except single-phase-to-ground. This applies whether conventional relays and current transformers, or the new linear-coupler scheme is used. The method is to connect the contacts of the supervising, or single-phase-to-ground fault-detecting relay, in series with the contacts of the main differential relay. If the latter relay then closes its contacts in error upon the occurrence of a heavy external two-phase-to-ground fault, for example, but the supervising relay properly maintains its contacts in the open position, no harm results. Up to now, no such supervising relay has been available which would recognize the existence of a single-phase-to-ground fault to the exclusion of all other types of faults. The new relay described in this paper is such a relay.

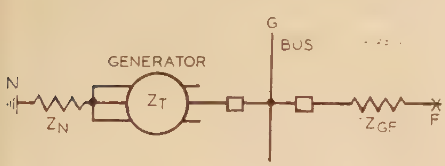

Figure 1. Schematic diagram of generator bus with fault on feeder circuit

Z_N—Grounding impedance
Z_T—Impedance of generator
Z_{GF}—Impedance from bus, G, to fault F
1, 2, 0—Where necessary, these subscripts are used in addition to those shown above to indicate the sequence network involved

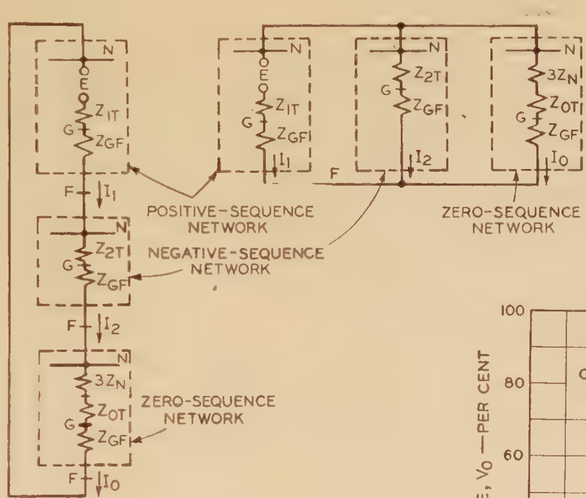

Figure 2. Sequence network connections for

(*a*). Single-phase-to-ground fault
(*b*). Two-phase-to-ground fault

Operating Principle

The new relay operates in accordance with the magnitude of the negative-sequence voltage and the zero-sequence voltage at the bus. The contact-closing force in the relay is developed in proportion to the zero-sequence voltage, while the restraining force opposing contact closing is proportional to the negative-sequence voltage.

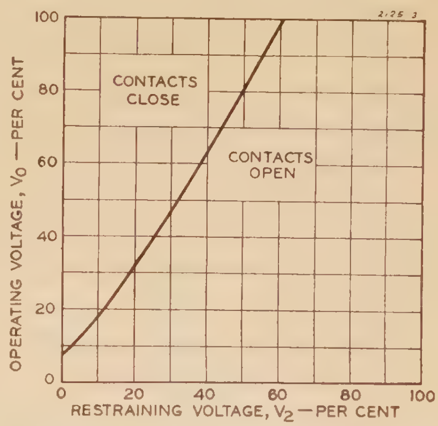

Figure 3. Typical operating characteristics of relay

The voltages shown are measured on the input side of the sequence filters
V_0—Zero-sequence voltage
V_2—Negative-sequence voltage

The proper application of the relay requires that the zero-sequence impedance of the system must exceed the negative-sequence impedance of the system by a reasonable margin. This requirement goes hand in hand with the need for the

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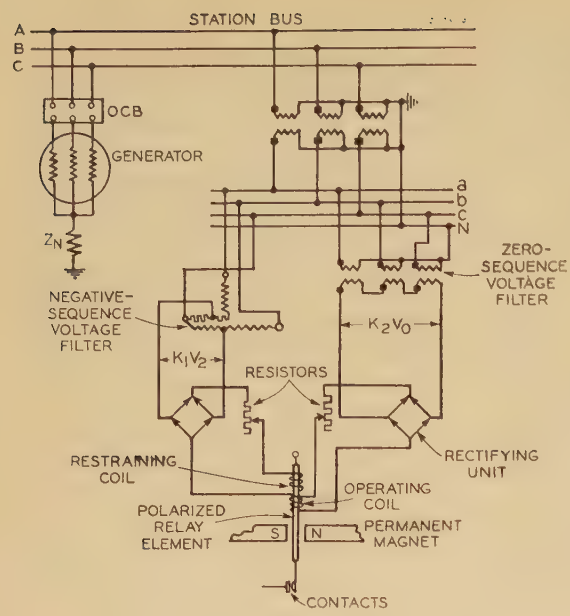


Figure 4 (left). Schematic diagram of connections of relay elements to station bus

K_1 and K_2 are design constants of the filter units

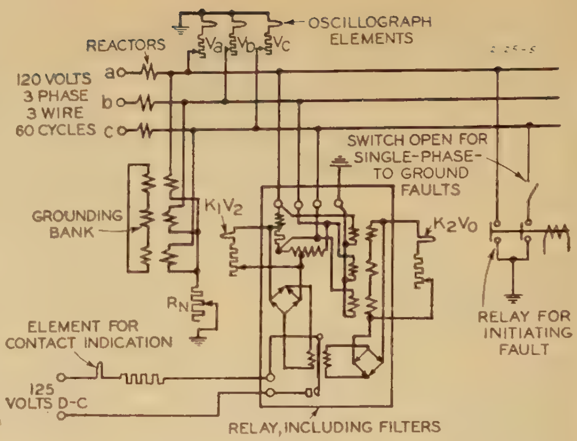


Figure 5 (right). Diagram of test connections

relay. On solidly grounded systems, where the ratio of zero-sequence to negative-sequence impedance at the generating station bus is low, ground-fault protection will generally be obtained by means of the phase differential relays, thus eliminating the need for the supervision of a nonexistent sensitive ground relay. When a grounding impedance is used, however, the need for the ground differential relay appears. At the same time, the use of the grounding impedance makes possible the application of the new supervising relay.

Figure 1 shows a schematic diagram of a generator bus with a fault on a feeder circuit. The generating capacity has been shown as an equivalent single generator. To make the case more general for the detailed derivations in the appendix, an impedance, Z_{GF}, has been assumed between the generator bus, G, and the fault at F.

Figure 2 shows the sequence network connections for single-phase-to-ground and two-phase-to-ground faults.[2] Phase-to-phase and three-phase faults need not be considered, inasmuch as no zero-sequence voltage is involved for these faults, and hence the relay would not experience any operating force. For convenience in simplifying the explanation of the relay operation, consider for the moment that the impedance, Z_{GF}, between the generator bus and the fault, F, is zero. This is equivalent to assuming that the fault occurs directly on the bus. Referring to Figure 2b for the two-phase-to-ground fault condition, it will be seen that the negative-sequence voltage to neutral at the bus is of necessity equal to the zero-sequence voltage to neutral, since the two networks are connected in parallel. Under this condition the relay will receive equal negative- and zero-sequences voltages from potential transformers connected to the bus and must not operate. The restraining coil of the relay must, therefore, be so proportioned as to be more powerful than the operating coil of the relay when both of these relay circuits receive equal voltages. Referring to Figure 2a for the single-phase-to-ground fault condition, it will be noted that the zero-sequence voltage will exceed the negative-sequence voltage in the same ratio as the impedance of the zero-sequence network exceeds that of the negative-sequence network. It will be noted that the value of the grounding impedance, Z_N, has been inserted in the zero-sequence network at three times its actual value in accordance with the conventional theorem. Since the same numerical value of current flows through the negative-sequence and zero-sequence network of Figure 2a, it follows that it is only necessary for the impedance of the zero-sequence network to be sufficiently high with respect to the impedance of the negative-sequence network to cause relay operation. The ohmic value of the grounding impedance, Z_N, will generally run quite high compared to the negative-sequence impedance of the generator, so that this condition is not difficult to meet.

Assume that the negative- and zero-sequence networks of Figure 2 are equal in ohmic value. It then follows that the negative- and zero-sequence voltages would be equal for each of these two cases. This is the limiting value in that if the relay-restraining and operating coils were so proportioned that the relay would be on the verge of closing, its contacts for a two-phase-to-ground fault, Figure 2b, then it would also be on the verge of closing its contacts for a single-phase-to-ground fault, Figure 2a. It follows more or less obviously that if Z_0 is greater than Z_2, the relay can be properly proportioned to discriminate between single-phase-to-ground and two-phase-to-ground short circuits. (Discrimination is automatic for phase-to-phase and three-phase short circuits, since no zero-sequence voltage is involved in these faults.)

It is desirable to establish a criterion for limits of application, and in this case it is felt that if the ratio Z_0/Z_2 is equal to or greater than 2/1, then a relay having characteristics similar to those illustrated in Figure 3, will meet the requirements satisfactorily.

Typical Application

Assume that Figure 1 represents a 13.2-kv installation and that the equivalent single generator has a rating of 20,000 kva. This is a relatively small capacity but is so chosen as to be on the pessimistic side in that the negative-sequence impedance will be relatively high. Typical values for the subtransient and negative-sequence reactance have been taken at 12 per cent each, and a zero-sequence reactance of four per cent has been assumed for the machine. The grounding impedance, Z_N, is assumed to be a two-ohm resistor, inasmuch as this is a value very frequently encountered. Using the above values of impedance and taking 7,620 volts to neutral as 100 per cent voltage, single-phase-to-ground and two-phase-to-ground faults on the bus were calculated. For the single-phase-to-ground fault, the negative-sequence voltage is 16.2 per cent and the zero-sequence voltage is 93.2 per cent. From the curve, Figure 3, when $V_2 = 16.2$ per cent, the required value of zero-sequence voltage, V_0, to operate the relay is 26 per cent. It is thus seen that the margin of the zero-

sequence voltage available over and above that required to barely operate the relay is ample. For the two-phase-to-ground fault, values of $V_2=49.5$ per cent and $V_0=49.5$ per cent were calculated for the assumed system. From the curve, it is found that at 49.5 per cent restraint voltage, the operating voltage, V_0, would have to be 79.5 per cent. Thus, the relay will not operate for the two-phase-to-ground fault. The calculations have been based on using the subtransient reactance of the machine in the positive-sequence network, since the relay is a high-speed relay.

While the relay has actually been made in the high-speed form, operating in one cycle or less, the principles involved are easily applied to other types of design so that the relay could be made "slow speed" if desired.

Figure 4 shows a schematic diagram of connections illustrating how the relay is energized from potential transformers connected to the station bus.

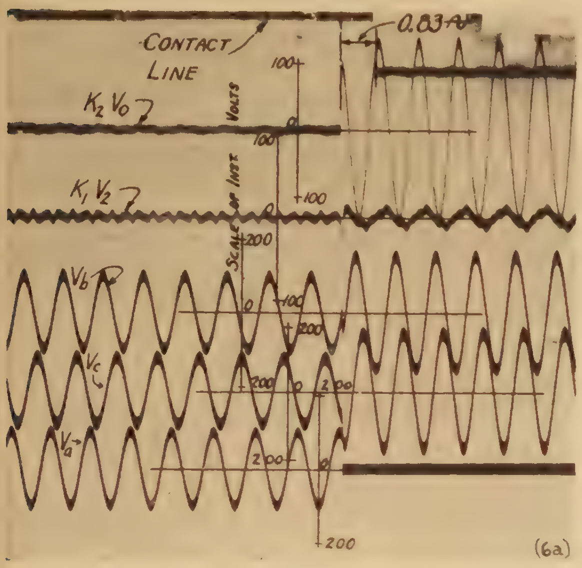
Tests

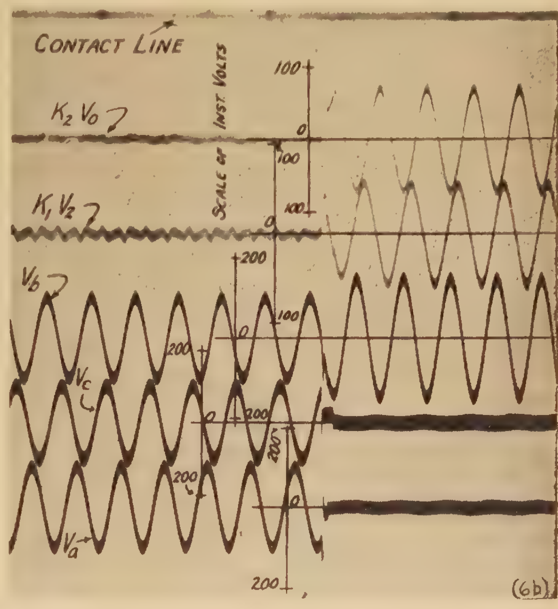
In order to check the actual operation of the relay against the theory, a test setup was made as shown in Figure 5. A system was represented in miniature. A variable resistor, R_N, was utilized as a grounding impedance in order that a minimum value for proper relay operation might be obtained and this checked against the calculations.

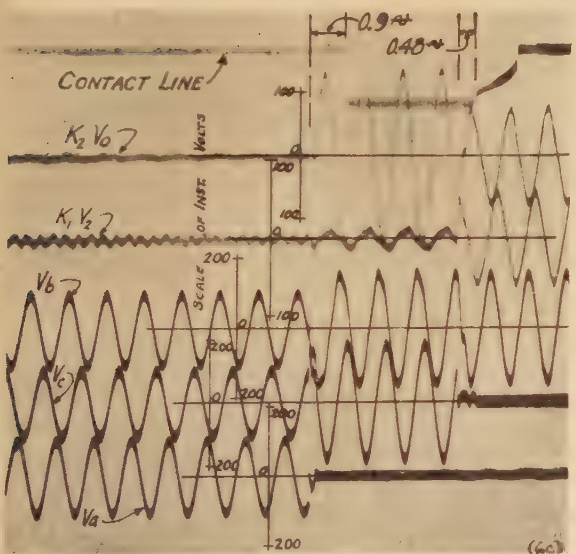
The location of oscillograph elements is shown in Figure 5. Elements were used to obtain traces of the phase-to-neutral voltages, V_a, V_b, and V_c, and the negative- and zero-sequence voltages, K_1V_2 and K_2V_0. The sixth element was used to obtain an indication of the operation of the relay contact. It should be noted that the output of the sequence filters is not numerically equal to the input quantities; hence the constants of proportionality, K_1 and K_2, have been used as indicated above.

Figure 6a shows the results of a test where phase *a* was faulted to ground. The ratio Z_0/Z_2 was 14.8 for this test. It will be observed that the relay properly closed its contact in 0.83 cycle. It will also be noted that there is a slight 60-cycle component and an appreciable harmonic component in the output of the negative-sequence filter prior to the fault. The 60-cycle component is explained by the fact that the filter was not perfectly balanced in its adjustment, and neither were the three-phase-to-neutral voltages perfectly balanced in the miniature setup. The existence of the harmonic component is occasioned by the fact that an iron core reactor is utilized in the filter design. In practice, the fact that the negative-sequence filter will have a combined output of fundamental and harmonic frequencies on the order of two volts or less under normal balanced conditions does not do any harm.

Figure 6b shows the results of a two-







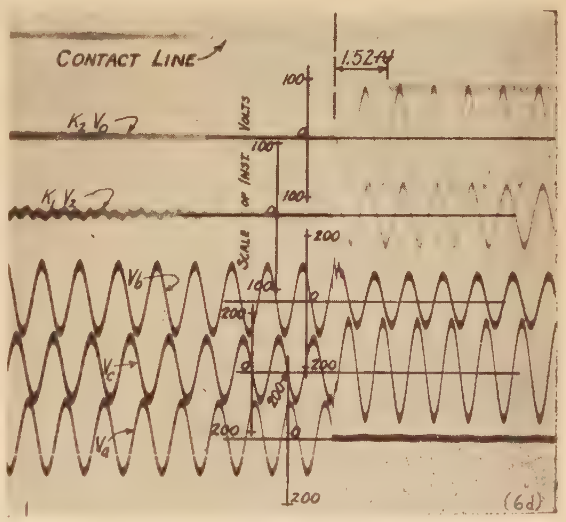


Figure 6. Test oscillograms

(a). Single-phase-to-ground fault $Z_0/Z_2=14.8$

(b). Two-phase-to-ground fault $Z_0/Z_2=14.8$

(c). Single-phase-to-ground fault developing into two-phase-to-ground $Z_0/Z_2=14.8$

(d). Single-phase-to-ground fault Minimum resistance in the neutral for correct operation of relay $Z_0/Z_2=1.7$

K_1V_2 = Negative-sequence voltage output of filter

K_2V_0 = Zero-sequence voltage output of filter

phase-to-ground fault on phases a and c. The ratio $Z_0/Z_2=14.8$ was kept the same as before. The calibration of the oscillograph elements remained the same, as in all cases to follow. The contacts of the relay did not operate. The relative magnitude of the deflections of the elements measuring K_1V_2 and K_2V_0 should be compared for Figures 6a and 6b.

In Figure 6c, the results are shown for a fault which started as one phase-to-ground and developed into a two-phase-to-ground fault. As before, the ratio of Z_0/Z_2 was kept at 14.8. The contacts first closed, as they should, in 0.9 cycle. When the second phase faulted to ground, the contacts opened in 0.48 cycle. The film indicates that the contacts were slow in opening the contact circuit. This is for the reason that the d-c current used in this element was kept high—just about at the limit that the relay contacts would successfully break. In practice, these contacts would not be expected to open heavy currents.

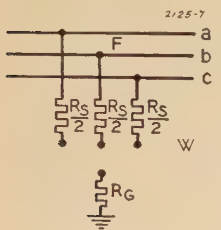


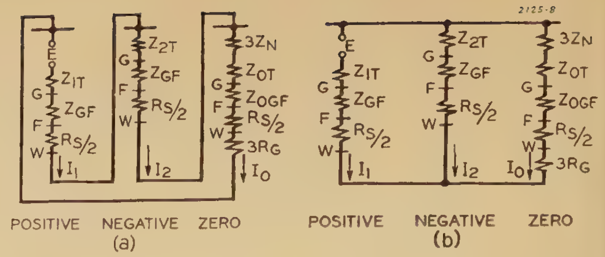
Figure 7. Diagram showing arc resistance at the point of fault

The resistance, R_N, was next reduced to such a value that the relay would just discriminate satisfactorily between one-phase- and two-phase-to-ground faults and the film, Figure 6d, was taken. For this case, the ratio Z_0/Z_2 was 1.7. Using the impedance values for the miniature system, a single-phase-to-ground fault condition was calculated. The calculated values were: $V_0=58.4$ per cent and $V_2=34.2$ per cent. At a negative-sequence restraining voltage of 34.2 per cent, the curve, Figure 3, indicates that the zero-sequence operating voltage required to just overcome this restraint should be not more than 54 per cent. An apparent error of 8.2 per cent is indicated in the calculations. This is explained on the basis that there were errors in the miniature setup which could be eliminated if it were found necessary to run the test with precision laboratory accuracy. The errors include the slight unbalance in the negative-sequence filter which was previously mentioned, a slight mutual coupling which existed between the reactors used in the supply circuit, and the fact that the burden of the sequence filters was appreciable with respect to other constants of the miniature system. None of these factors was taken into consideration in the calculations.

Figure 8. Connections of sequence networks for

(a). Single-phase-to-ground fault

(b). Two-phase-to-ground fault



In Figure 6d, the contact closing time 1.52 cycles under minimum operating conditions is still reasonably fast.

Conclusion

A relay design is now available which will properly discriminate between single-phase-to-ground faults and all other types of faults on grounded systems when the ratio of the zero-sequence impedance of the system to the negative-sequence impedance is greater than unity.

Appendix

Let it be assumed that the fault occurs at the point, F, of Figure 1, and that the arc resistances are represented as shown in Figure 7. The sequence network connections are shown by Figure 8 for the single-phase-to-ground and two-phase-to-ground faults. The points G, F, and W are shown in these diagrams.

Case I—Single-Phase-to-Ground Fault

Referring to Figure 8a, let

$$\boldsymbol{Z}_1=\boldsymbol{Z}_{1T}+\boldsymbol{Z}_{GF}+\boldsymbol{R}_s/2 \tag{1}$$

$$\boldsymbol{Z}_2=\boldsymbol{Z}_{2T}+\boldsymbol{Z}_{GF}+\boldsymbol{R}_s/2 \tag{2}$$

and $$\boldsymbol{Z}_0=3\boldsymbol{Z}_N+\boldsymbol{Z}_{0T}+\boldsymbol{Z}_{0GF}+\boldsymbol{R}_{s/2}+3\boldsymbol{R}_G \tag{3}$$

In the series connections of networks

$$\boldsymbol{I}_1=\boldsymbol{I}_2=\boldsymbol{I}_0 \tag{4}$$

and $$\boldsymbol{I}_1=\frac{\boldsymbol{E}}{\boldsymbol{Z}_1+\boldsymbol{Z}_2+\boldsymbol{Z}_0} \tag{5}$$

The negative- and zero-sequence voltage drops at the bus, G, are

$$\boldsymbol{E}_{2G}=0-\boldsymbol{I}_2\boldsymbol{Z}_{2T} \tag{6}$$

and $$\boldsymbol{E}_{0G}=0-\boldsymbol{I}_0(3\boldsymbol{Z}_N+\boldsymbol{Z}_{0T}) \tag{7}$$

Substituting $\boldsymbol{I}_2$ for $\boldsymbol{I}_0$ (since these are equal, equation 4) in equation 7, and dividing equation 6 by equation 7, a ratio, R_1, is obtained

$$R_1=\frac{\boldsymbol{E}_{2G}}{\boldsymbol{E}_{0G}}=\frac{\boldsymbol{Z}_{2T}}{(3\boldsymbol{Z}_N+\boldsymbol{Z}_{0T})} \tag{8}$$

Equation 8 gives the relationship existing between the negative-sequence restraining voltage and the zero-sequence operating voltage when the relay must operate.

Case II—Two-Phase-to-Ground Fault

Referring to Figure 8b, let equations 1, 2, and 3 stand as before. In this case, however

$$\boldsymbol{I}_1=\frac{\boldsymbol{E}(\boldsymbol{Z}_0+\boldsymbol{Z}_2)}{\boldsymbol{Z}_1\boldsymbol{Z}_0+\boldsymbol{Z}_1\boldsymbol{Z}_2+\boldsymbol{Z}_2\boldsymbol{Z}_0} \tag{9}$$

$$\boldsymbol{I}_2=-\boldsymbol{I}_1\frac{\boldsymbol{Z}_0}{\boldsymbol{Z}_2+\boldsymbol{Z}_0} \tag{10}$$

$$\boldsymbol{I}_0=-\boldsymbol{I}_1\frac{\boldsymbol{Z}_2}{\boldsymbol{Z}_2+\boldsymbol{Z}_0} \tag{11}$$

As in Case I,

$$\boldsymbol{E}_{2G}=0-\boldsymbol{I}_2\boldsymbol{Z}_{2T} \tag{6}$$

and $$\boldsymbol{E}_{0G}=0-\boldsymbol{I}_0(3\boldsymbol{Z}_N+\boldsymbol{Z}_{0T}) \tag{7}$$

Substituting for $\boldsymbol{I}_2$ and $\boldsymbol{I}_0$ in terms of $\boldsymbol{I}_1$, equations 6 and 7 become

$$\boldsymbol{E}_{2G}=0+\boldsymbol{I}_1\frac{\boldsymbol{Z}_0}{\boldsymbol{Z}_2+\boldsymbol{Z}_0}\boldsymbol{Z}_{2T} \tag{12}$$

$$\boldsymbol{E}_{0G}=0+\boldsymbol{I}_1\frac{\boldsymbol{Z}_2}{\boldsymbol{Z}_2+\boldsymbol{Z}_0}(3\boldsymbol{Z}_N+\boldsymbol{Z}_{0T}) \tag{13}$$

A new ratio, R_2, between the negative-sequence restraining voltage and zero-sequence operating voltage, is obtained by dividing equation 12 by equation 13

$$R_2=\frac{\boldsymbol{Z}_0\boldsymbol{Z}_{2T}}{\boldsymbol{Z}_2(3\boldsymbol{Z}_N+\boldsymbol{Z}_{0T})} \tag{14}$$

Summary

If the relay is to discriminate properly between the conditions of case I and case II, then the ratio, R_2, must be greater than the ratio, R_1. Dividing equation 14 by equation 8, there results

$$R_2/R_1=\frac{Z_0}{Z_2} \tag{15}$$

Equation 15 indicates that if $Z_0/Z_2>1$, then $R_2/R_1>1$, and the relay will receive more restraining voltage in proportion to operating voltage for the two-phase-to-ground fault condition than it will for the single-phase-to-ground fault.

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On Eddy Currents in a Rotating Disk

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A DEVICE which often occurs in electric machines and instruments consists of a relatively thin conducting disk rotating between the pole pieces of a permanent magnet or electromagnet. The author has received inquiries as to the method of calculating the paths of the eddy currents and the torque in such cases. The following rather simple method, which is quite accurate for a permanent magnet, seems not to be described in the literature. It assumes that the disk is so thin that the skin effect can be neglected. This is true for all frequencies that can be produced mechanically. To facilitate calculation in the special case of circular poles it is also as-

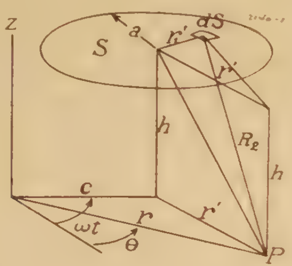

Figure 1. Geometrical relations for derivation of formulas for stream function

sumed that $2\pi\omega ab\gamma=\epsilon a$ is much less than one where ω is the angular frequency of rotation in radians per second, a the pole-piece radius, b the disk thickness, and γ the electric conductivity, all in centimeter-gram-second electromagnetic units. This produces a fractional error of less than ϵa in the eddy current densities and of less than $(\epsilon a)^2$ in the torque. In the case of the electromagnet the situation is complicated by the presence of the permeable pole pieces in the magnetic field of the eddy currents. This may send a large demagnetizing flux through the electromagnet. An approximate solution for this case will be considered.

Maxwell's Formula

This calculation starts from a formula given by Maxwell in 1873,[1] but apparently little known to engineers. To apply it one should know its derivation which, as given by Maxwell, is difficult for modern students to follow. A simplified proof which brings out the points essential for our problem is given below.

The object is to calculate the magnetic induction $\boldsymbol{B}$ produced by the eddy currents of density $\boldsymbol{i}$ induced in a thin plane sheet of thickness b, unit permeability and conductivity γ lying in the xy plane by a fluctuating magnetic field of induction $\boldsymbol{B'}$. Evidently the only components of $\boldsymbol{i}$ effective in producing magnetic effects parallel its surface. Let the eddy currents be confined to a finite region of the sheet which may or may not extend to infinity, and let us define the stream function $U(x, y)$ at any point in the sheet to be the current flowing through any cross section of the sheet extending from P to its edge. The line integral of $\boldsymbol{B}$ or $\boldsymbol{H}$ over the closed path that bounds this section equals $4\pi U$. From symmetry the contribution from the upper and lower halves of the path is the same so we may write

$$4\pi U=\oint \boldsymbol{B}\cdot d\boldsymbol{s}=\pm 2\int_x^\infty B_x dx$$
$$=\pm 2\int_y^\infty B_y dy \tag{1}$$

where the choice of sign depends on the side of the sheet chosen for the integration. Differentiating this equation gives

$$bi_x=\frac{\partial U}{\partial y}=\pm\frac{B_y}{2\pi} \qquad bi_y=-\frac{\partial U}{\partial x}=\mp\frac{B_x}{2\pi} \tag{2}$$

These equations connect the eddy current density with the tangential components of the magnetic induction $\boldsymbol{B}$ produced by $\boldsymbol{i}$ at the surface of the sheet.

The eddy currents are generated not only by the changes in the magnetic induction $\boldsymbol{B'}$ of the external field, but also by the changes of the magnetic induction $\boldsymbol{B}$ of eddy currents elsewhere in the sheet. One of Maxwell's equations combined with Ohm's law gives the induced current to be

$$\nabla\times\boldsymbol{E}=\nabla\times\frac{\boldsymbol{i}}{\gamma}=-\frac{\partial}{\partial t}(\boldsymbol{B'}+\boldsymbol{B}) \tag{3}$$

Writing out the z component of this equation and using equation 2 give

$$\frac{1}{\gamma}\left(\frac{\partial i_y}{\partial x}-\frac{\partial i_x}{\partial y}\right)=\mp\frac{1}{2\pi b\gamma}\left(\frac{\partial B_x}{\partial x}+\frac{\partial B_y}{\partial y}\right)$$
$$=-\frac{\partial}{\partial t}(B_z'+B_z) \tag{4}$$

Another of Maxwell's equations states that

$$\nabla\cdot\boldsymbol{B}=\frac{\partial B_x}{\partial x}+\frac{\partial B_y}{\partial y}+\frac{\partial B_z}{\partial z}=0 \tag{5}$$

Combining equations 4 and 5 gives

$$\pm\frac{\partial(B_z'+B_z)}{\partial t}=\frac{1}{2\pi b\gamma}\frac{\partial B_z}{\partial z} \tag{6}$$

When $\partial B_z'/\partial t$ is known, this equation gives the boundary condition on $\boldsymbol{B}_z$ in the plane of the sheet. This, combined with the equations $\nabla\times\boldsymbol{B}=0$ and $\nabla\cdot\boldsymbol{B}=0$ which hold outside the sheet, and the fact that $\boldsymbol{B}$ vanishes at infinity serves to determine $\boldsymbol{B}$ everywhere. By equations 1 and 2 the current density and stream function anywhere in the sheet can be found.

The explicit expression for $\boldsymbol{B}$ in terms

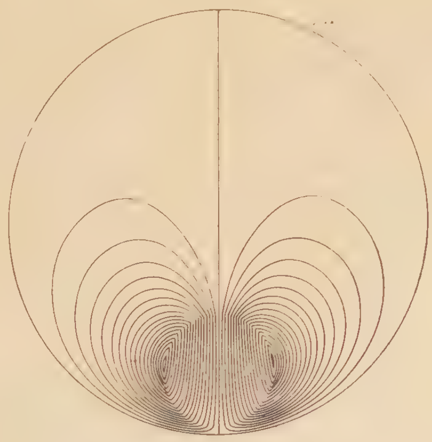

Figure 2. Lines of flow of eddy currents induced in rotating disk by single circular magnet pole

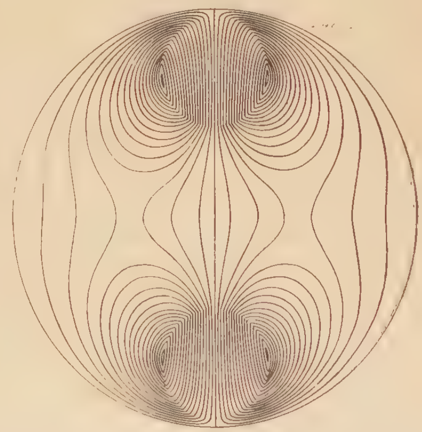

Figure 3. Lines of flow of eddy currents induced in rotating disk by two circular magnet poles

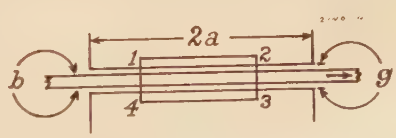

Figure 4. Geometrical relations for calculation of demagnetizing flux

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of $\boldsymbol{B}'$ which was given by Maxwell[1] can be obtained as follows. The right side of equation 6 is finite at all times which means that if $\Delta t \to 0$ then $\Delta(B_z'+B_z) \to 0$. Thus an abrupt change in $\boldsymbol{B}'$ instantaneously induces eddy currents such as will maintain $\boldsymbol{B}'+\boldsymbol{B}$ unchanged in the sheet. Therefore, for a specified change in $\boldsymbol{B}'$ the initial value of $\boldsymbol{B}$ is known, and, if no further changes in $\boldsymbol{B}'$ occur its subsequent values as the eddy currents decay are found by putting $\partial \boldsymbol{B}'/\partial t = 0$ in equation 4 and solving. A second abrupt change in $\boldsymbol{B}'$ produces a second set of eddy currents, and so forth. At any instant the actual field of the eddy currents is a superposition of these. As the magnitudes of the discontinuous changes in the external field become smaller, and the intervals between them shorter, we approach as a limit a continuously changing magnetic field.

Suppose that the sources of the inducing field lie above the xy plane where $z>0$. At $t=0$ the source changes abruptly the induction being $\boldsymbol{B}_1' = \boldsymbol{F}_1(x, y, z)$ when $-\infty < t < 0$ and $\boldsymbol{B}_2' = \boldsymbol{F}_2(x, y, z)$ when $0 < t < \infty$. As just shown the eddy currents generated at $t=0$ initially keep the field on the negative side of an infinite sheet unchanged. When $z<0$ we have therefore

$$\boldsymbol{B}_{t=0} = \boldsymbol{B}_1' - \boldsymbol{B}_2' = \boldsymbol{F}_1(x, y, z) - \boldsymbol{F}_2(x, y, z) \quad (7)$$

Since $\boldsymbol{B}_2'$ is not a function of t, equation 6 reduces to

$$\pm\frac{\partial B_z}{\partial t} = \frac{1}{2\pi b\gamma}\frac{\partial B_z}{\partial z} \quad (8)$$

These equations, 7 and 8, are satisfied by

$$\boldsymbol{B} = \boldsymbol{F}_1\left(x, y, z \pm \frac{t}{2\pi b\gamma}\right) - \boldsymbol{F}_2\left(x, y, z \pm \frac{t}{2\pi b\gamma}\right) \quad (9)$$

Because the eddy currents must die out, and their magnetic field must be symmetrical about the sheet, we take the plus sign when z is positive and the negative sign when z is negative. Thus equation 9 shows that, in addition to $\boldsymbol{B}_2'$ which would exist if no sheet were present, there is a decaying field due to eddy currents which appears, from either side of the sheet, to be caused by a pair of images receding with uniform velocity $1/(2\pi b\gamma)$. Suppose our inducing field has the form

$$\boldsymbol{B}' = \boldsymbol{F}(t, x, y, z) \quad (10)$$

The change in this field in an infinitesimal time interval $d\tau$ is given by

$$\frac{\partial \boldsymbol{B}'}{\partial t}d\tau = \frac{\partial}{\partial t}\boldsymbol{F}(t, x, y, z)d\tau \quad (11)$$

The initial field of the eddy currents formed in that interval must be equal and opposite to this and must die out as if their source moved away with a uniform speed $1/(2\pi b\gamma)$. Thus the eddy currents at a time t due to a change in the interval $d\tau$ at a time τ before t is given by

$$d\boldsymbol{B} = -\frac{\partial}{\partial t}\boldsymbol{F}\left(t-\tau, x, y, z \pm \frac{\tau}{2\pi b\gamma}\right)d\tau \quad (12)$$

This is Maxwell's formula. It has many applications.[2] When the field is produced by moving permanent magnets, it is convenient to express U in terms of the scalar magnetic potential Ω. Since we have unit permeability we may write

$$U = \frac{1}{2\pi}\int_r^\infty B_r dr = \frac{-1}{2\pi}\int_r^\infty \frac{\partial \Omega}{\partial r}dr = \frac{\Omega}{2\pi} \quad (13)$$

Application to Magnet Moving in a Circle

We now take the case of a magnetic field produced by a long right circular cylinder of radius a, uniformly and permanently magnetized parallel to its axis, so as to give a total flux Φ. The magnetic pole density in the face is therefore $\Phi/(2\pi a)^2$. This magnet moves in a circle with a uniform angular velocity ω its axis

being c centimeters from the z axis, and its lower end h centimeters above the $z=0$ plane in which lies an infinite plane sheet of thickness b and conductivity γ. Its upper end is too remote for consideration. Polar co-ordinates will be written unprimed or primed according as they refer to the axis of rotation or to the pole-piece axis. The scalar magnetic potential Ω' of its lower face, of area S, at the point P is seen from Figure 1 to be

$$\Omega' = \frac{\Phi}{(2\pi a)^2}\int_S \frac{dS}{R_2} = \frac{\Phi}{(2\pi a)^2}\times \int_0^a\int_0^{2\pi}\frac{r_1'dr_1'd\theta'}{\sqrt{h^2+r_1'^2+r'^2-2r_1'r'\cos\theta'}}$$

where $r'^2 = r^2+c^2-2rc\cos(\omega t+\theta)$. This combined with equations 12 and 13 give the stream function to be

$$U = \frac{\Phi}{(2\pi)^3 a^2}\int_0^a\int_0^{2\pi}\int_0^\infty \frac{\partial}{\partial t}\times \left(\frac{r_1'dr_1'd\theta'd\tau}{\sqrt{h_\tau^2+r_1'^2+R_\tau'^2-2r_1'R_\tau'\cos\theta'}}\right) \quad (14)$$

where $R_\tau'^2 = r^2+c^2-2rc\cos(\omega(t-\tau)+\theta)$ and $h_\tau = h+\tau/(2\pi b\gamma) = h+u$. Let us now bring the pole piece down close to the plate so that $h_\tau = \tau/(2\pi b\gamma)$, and bring up a similar pole of opposite sign from the other side, so that the eddy current density is doubled. We now carry out the differentiation with respect to t and set $t=0$ so that the $\theta=0$ line bisects the pole piece when $t=0$. The integral then becomes

$$U = \frac{rc\Phi\epsilon}{\pi(2\pi a)^2}\int_0^a\int_0^{2\pi}\int_0^\infty \frac{\sin(\theta+\epsilon u)(R_u'-r_1'\cos\theta')r_1'dr_1'd\theta'du}{R_u'(u^2+r_1'^2+R_u'^2-2R_u'r_1'\cos\theta')^{3/2}} \quad (15)$$

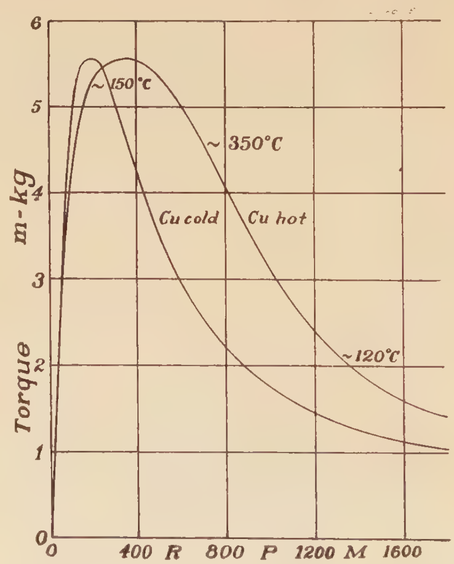

Figure 5. Curves showing torque versus speed for large disk rotating between the four rectangular pole pairs of an electromagnet, measured by Lentz

where $\epsilon = 2\pi\omega b\gamma$ and $R_u'^2 = r^2+c^2-2rc\times\cos(\theta+\epsilon u)$. For 3,000 rpm with a copper sheet 0.25 millimeter thick $\epsilon \approx 0.01$ so that u^2 in the denominator has reached the value 100 when ϵu reaches 0.1. In calculating such a quantity as the torque where the current density is integrated over the pole piece, the neglect of ϵ produces a fractional error less than $(\epsilon/a)^2$, so that the result should be good to one per cent for a sheet one millimeter thick. We may therefore drop the ϵ terms so that R_u' becomes the r' in Figure 1 and integrate with respect to u giving

$$U = \frac{K\sin\theta}{r'}\times \int_0^a\int_0^{2\pi}\frac{(r'-r_1'\cos\theta')r_1'dr_1'd\theta'}{r'^2+r_1'^2-2r'r_1'\cos\theta'} \quad (16)$$

where we have written K for the coefficient of the integral in equation 15. The integral with respect to θ', from Dwight's table of integrals 860.2, is zero when $r'<r_1'$ and π/r' when $r'>r_1'$. Thus the upper limit for the r_1' integration is a when $r'>a$ and r' when $r'<a$, which gives

$$r'>a \qquad U = \frac{\omega rcb\gamma\Phi\sin\theta}{2\pi r'^2} \quad (17)$$

$$r'<a \qquad U = \frac{\omega rcb\gamma\Phi\sin\theta}{2\pi a^2} \quad (18)$$

The next question is how to restrict the eddy currents to the interior of the disk bounded by the circle $r=A$. We observe that if we use equation 15 to calculate U for a second magnet also carrying a flux Φ but with circular pole pieces of radius $a''=Aa/c$ centered at $c''=A^2/c$, so that $R_u''^2=r^2+(A^2/c)^2-2r(A^2/c)\cos(\theta+\epsilon u)$, and change the variables of integration from r_1' to $r_1'A/c$, and from u to Au/c, then the resultant expression is identical with equation 15, except that we have cR_u''/A instead of R_u' and $A\epsilon/c$ instead of ϵ. But when $r=A$ we see that $cR_u''/A=R_u'$, so that both magnets, one outside and one inside the circle $r=A$, give the same U on this circle. Furthermore by taking the air gap in each magnet small, the fluxes are confined to the areas under the pole pieces, so that neither induces directly eddy currents on the other side of the circle $r=A$. It is evident that if the fluxes from the two magnets cut the sheet in opposite directions, then $U=0$ when $r=A$ and the currents induced by the inner pole are kept inside the circle. This is exactly the boundary condition for a disk of radius A, except that the calculated system includes the currents induced in the region $r<A$ by the magnetic field of the eddy currents in the region $r>A$, which does not exist in the case of the disk. This field is proportional to $\Phi\epsilon$ which is, by hypothesis, small compared with Φ, and in addition the source is further away, so that the fractional error in U will be less than ϵ. We should note also from the symmetry that the radial component of these secondary currents is opposite in sign on the two sides of the $\theta=0$ line, so that their effect cancels out completely in calculating the torque which therefore should be accurate to terms in ϵ^2. The contribution to U from the outer magnet is found by putting $c^2R''^2/A^2$ for R^2 in equation 17. Adding this to equations 17 and 18, we obtain for the stream function of the eddy currents in the disk

$$R>a \quad U=\frac{\omega rcb\gamma\Phi\sin\theta}{2\pi}\left(\frac{1}{r^2+c^2-2rc\cos\theta}-\frac{A^2}{c^2r^2+A^4-2rcA^2\cos\theta}\right) \tag{19}$$

$$R<a \quad U=\frac{\omega rcb\gamma\Phi\sin\theta}{2\pi a^2}\left(1-\frac{A^2a^2}{c^2r^2+A^4-2rcA^2\cos\theta}\right) \tag{20}$$

The torque may be calculated by integrating the product of the radial component of the current by the magnetic induction and by the lever arm and integrating over the area S of the pole piece. Thus, using equation 2, we have

$$T=\int_S\frac{rbi_r\Phi}{\pi a^2}dS=\frac{\Phi}{\pi a^2}\int_{c-a}^{c+a}\int_{-\theta_1}^{+\theta_1}r\frac{\partial U}{\partial r}rdrd\theta$$

where θ_1 and r are connected by the relation $r^2+c^2-2rc\cos\theta_1=a^2$. Substituting for U from equation 20 and integrating with respect to θ give

$$T=\frac{\omega cb\gamma\Phi^2}{\pi^2a^4}\times\int_{c-a}^{c+a}\left(r^2\sin\theta_1-\frac{a^2A^2r^2\sin\theta_1}{c^2r^2+A^4-2A^2rc\cos\theta_1}\right)dr \tag{21}$$

The integration is simplified by taking a new variable u so that $4acu^2=r^2-(c-a)^2$ which gives the limits 0 and 1. Thus we obtain, writing out ϵ,

$$T=\frac{\omega b\gamma\Phi^2c^2}{2\pi a^2}\left(1-\frac{A^2a^2}{(A^2-c^2)^2}\right)=\omega\gamma\Phi^2D_1 \tag{22}$$

This formula gives the torque in dyne centimeters when ω is in radians per second, Φ in maxwells, a, b, c, and A in centimeters and γ in electromagnetic units. If we are given the volume resistivity ρ of the disk in ohm-centimeters $\gamma=10^{-9}/\rho$.

If the magnet is fixed, and the disk rotates, the arrangement described exerts an undesired force on the disk axis which may be avoided by using two identical magnets on opposite sides of the axis and equidistant from it. This approximately doubles the torque given by equation 22. The additional torque from the eddy currents of one magnet flowing under the poles of the other may be found by an integral similar to equation 21 which is

$$T'=\frac{\omega b\gamma\Phi^2c}{\pi^2a^2}\int_{c-a}^{c+a}\left(\frac{r^2\sin\theta_1}{r^2+c^2+2rc\cos\theta_1}-\frac{r^2A^2\sin\theta_1}{r^2c^2+A^4+A^2rc\cos\theta_1}\right)dr \tag{23}$$

Integrating by the same substitutions as equation 21, adding to equation 22 and multiplying by two give

$$T=\frac{\omega b\gamma\Phi^2c^2}{\pi a^2}\left(\frac{4c^2+a^2}{4c^2}-\frac{2a^2A^2(A^4+c^4)}{(A^4-c^4)^2}\right)=\omega\gamma\Phi^2D_2' \tag{24}$$

This holds when the two magnet fields are antiparallel. If we subtract the integral of equation 23 from equation 22 and multiply by two we get

$$T=\frac{\omega b\gamma\Phi^2c^2}{\pi a^2}\left(\frac{4c^2-a^2}{4c^2}-\frac{4a^2c^2A^4}{(A^4-c^2)^2}\right)=\omega\gamma\Phi^2D_2'' \tag{25}$$

This holds when the two magnetic fields are parallel. The arrangement of equation 24 gives more torque than that of equation 25. The eddy-current flow lines corresponding to constant values of U as calculated from equations 19 and 20 applied to the cases of equations 22 and 24 are shown in Figures 2 and 3 where $a=\sqrt{7}$ cm, $c=7$ cm, $A=10$ cm and $\omega b\gamma\Phi/(2\pi)=3.5$. The value U on the outer boundary is zero and changes by steps of one in Figure 2 and steps of two in Figure 3.

Demagnetizing Effects

So far the magnet pole pieces have been assumed to be so hard that they do not short-circuit the flux of the eddy currents. This is not true for the permeable pole pieces of an electromagnet, whose effect may be calculated approximately by observing that the current $2U$ is enclosed by the rectangular path 1-2-3-4-1 in Figure 4, which lies in the upper and lower pole pieces except where it cuts across the disk and gap normally at $r=r_1$ and $\theta=\pm\theta_1$. If the reluctance of this circuit lies entirely in the air gaps, each of length g, then the magnetic flux density B_e due to the eddy currents alone at r_1, $\pm\theta_1$ is $4\pi U/g$. Substituting for U from equation 20 and writing as before $\epsilon=2\pi\omega b$ gives

$$B_e=\frac{c\epsilon r_1\Phi\sin\theta_1}{\pi a^2g}\left(1-\frac{A^2a^2}{c^2r_1^2+A^4-2r_1cA^2\cos\theta_1}\right) \tag{26}$$

This shows that when b and g are comparable in size B_e cannot be neglected compared with the original flux density $\Phi/(\pi a^2)$. The $\sin\theta_1$ term shows that the radial component of the eddy currents induced by B_e have opposite signs under the two halves of the pole piece, so that they contribute nothing directly to the torque, but on the other hand they form closed circuits about the central portion and so produce a demagnetizing magnetomotive force in the electromagnet. The

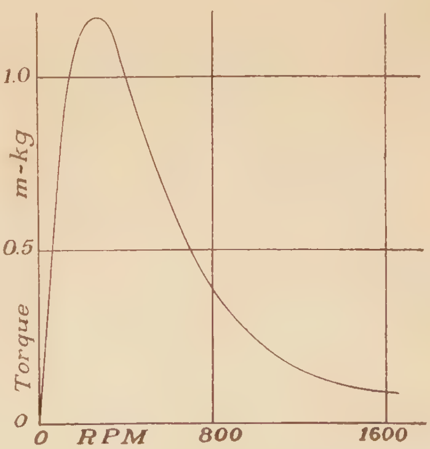

Figure 6. Curves showing torque versus speed for a large disk rotating between the single pair of circular pole pieces of an electromagnet as given by equations 22, 27, and 28

stream function U_e of these eddy currents is calculated as U is, but to simplify matters we carry out the operations from equations 14 to 16 for a single element of the pole face, along with its image element outside the circle $r=A$. We then give each element the strength indicated in equation 26 and set up a definite integral for U over the pole-piece area. This method is less exact than setting up equation 14 for the whole face, because it ignores that part of the flux threading dS from the current induced by B_e outside this area, which is of the order $\epsilon B_e dS$. The eddy currents U_e are evidently equivalent to a magnetic shell of variable strength U_e in the sheet and to get $\mathfrak{F}_e$ the demagnetizing magnetomotive force we must find the equivalent uniform shell. Thus we have

$$\mathfrak{F}_e = \frac{1}{\pi a^2}\int_S U_e dS$$

where S is the area of the pole face. We now have a complicated quadruple integral involving the variables r', R', θ' and θ_1' whose evaluation can be simplified somewhat by integrating in the proper order. The result is

$$\mathfrak{F}_e = \frac{\omega^2 b^2\gamma^2 c^2}{4g}\Phi\left(1-\frac{2a^2A^2(A^2+c^2)}{c^2(A^2-c^2)^2}+\frac{2A^4}{c^4}\log_e\frac{(A^2-c^2)^2}{(A^2-c^2)^2-c^2a^2}\right)=\beta^2\gamma^2\omega^2\Phi \quad (27)$$

If the flux penetrating the sheet at rest is Φ_0, then when in motion we have, if $\mathfrak{R}$ is the reluctance of the electromagnet, $\Phi = \Phi_0-\beta^2\gamma^2\omega^2\Phi/\mathfrak{R}$, so that

$$\Phi = \frac{\mathfrak{R}\Phi_0}{\mathfrak{R}+\beta^2\gamma^2\omega^2} \quad (28)$$

The expressions for the torque now become

$$T = \frac{\omega\gamma\mathfrak{R}^2\Phi_0^2 D}{(\mathfrak{R}+\beta^2\gamma^2\omega^2)^2} \quad (29)$$

where D has the values given in equations 22, 24, or 25, according to the pole arrangement. There is now a definite speed for maximum torque which is found by setting $\partial T/\partial\omega=0$ to be

$$\omega_m = \frac{1}{\beta\gamma}\sqrt{\frac{\mathfrak{R}}{3}} \quad (30)$$

Putting this in equation 29 gives

$$T_m = \frac{3\sqrt{3\mathfrak{R}}\Phi_0^2 D}{16\beta} \quad (31)$$

This is independent of the conductivity which is surprising, although there is some evidence for it in Lentz's experimental curves shown in Figure 5 which give hot and cold copper disks the same T_m for different ω_m.

These calculations of demagnetizing effects have been worked out for a single pole. For an even number of poles with alternating signs, we have seen that the torque per pole is increased, but the demagnetizing forces are also increased so that the torque obtained by multiplying equation 29 by the number of poles will probably not be far wrong. The speed for maximum torque given by equation 30 will certainly be decreased, perhaps considerably, because of the increase in β.

The only formula we can find for this torque is one derived by Rüdenberg.[3] This formula is written as a double infinite series and is derived by considering a thin conducting strip bounded by straight lines which moves lengthwise in the narrow gap between magnetic poles with rectangular faces. The fields of adjacent poles are antiparallel, so that the inducing fields can be expanded in a double series of odd harmonics. This formula was checked qualitatively by Zimmermann,[4] but could not be verified quantitatively, as the theoretical and experimental boundary conditions did not agree. Lentz found only those terms involving the lengthwise harmonics were of importance and dropped the rest. His experimental brake had the center of the disk removed to simulate a ring whose width roughly equaled that of the postulated strip. His four poles were so far apart that their action was nearly independent. We have redrawn in Figure 5, his experimental curves giving the torque in meter kilograms against angular velocity in revolutions per second. The ring had inner and outer radii of 5 centimeters and 25 centimeters and was 0.4 centimeter thick. The air space was 1.2 centimeters, and the centers of the rectangular pole pieces were 20.75 centimeters from the rotation axis and were 6 centimeters (radial) by 8 centimeters (tangential). The inducing field was 2,150 gauss at rest. The figures on the hot copper curve show the estimated stable mean temperature for that speed.

A direct quantitative comparison of our formula with Lentz's data is difficult, because he used rectangular poles, his air gap was so large as to spread the inducing field over an unknown area, the center of his disk was cut away, and we do not know where his flux density was measured. Although our formulas are inaccurate for such large dimensions at the high speeds, it is interesting to see what results they give for a comparable case. Let us take $b=0.4$ cm, $A=25$ cm, $a=4$ cm, $c=21$ cm, $g=0.6$ cm, $B=2{,}000$ gauss and assume the reluctance entirely in the air gap. In equation 22, $D_1=1.23$, in equation 27, $\beta=3.85$ and in equation 28 $\mathfrak{R}=0.012$. The angular velocity for maximum torque for copper ($\gamma=1/1{,}700$) is given by equation 30 to be 27.9 radians per second or 267 rpm. T_m is 1.15×10^8 dyne cm or 1.17 kilogram-meters for this single pole and roughly four times this for four poles. Expressing T' in kilogram-meters and ω' in rpm, equation 29 becomes

$$T' = \frac{0.00785\omega'}{(1+0.0000047\omega'^2)^2}\text{kg-m}$$

This formula is plotted in Figure 6. A comparison of Figure 5 with Figure 6 indicates that our formula gives too rapid a falling off in torque at high speeds. It should be pointed out that other conditions, such as the degree of saturation of the iron in the magnet will upset the assumed relation between magnetomotive force and Φ and may modify equations 28, 29, 30, and 31 considerably.

The methods given in this paper may be extended to any number of poles by the method used for two and to other than circular faces. Several such calculations have been carried out, but it is doubtful if the additional theoretical accuracy justifies publishing them. The difference between the ideal boundary conditions used here and those found in apparatus is such that we recommend that the torque for one pole be calculated by equation 22 for permanent magnets or by equation 29 for electromagnets, and the result multiplied by the number of poles to give the total torque. In power apparatus the heating of the disk will change its resistivity and may cause it to expand and buckle and otherwise upset the calculations.

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Poles and Pole Treatment

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TO write about poles and the preservative treatment of poles today is like writing the review of a very successful play that has been running a long time. The players are not men, but inanimate poles. The natural backdrop is the forest. The stage is as broad as the United States and the setting is a criss-cross of thousands of miles of utility and communication lines. The engineers of these lines are the producers and directors of the play; and it is they who assign the parts to the various actors and write a changing script as the play progresses. It is the purpose of this paper to present certain highlights of the more important scenes, such as pole use and the drain on the forest, pole manufacture and treatment under standard specifications, and pole strength, and to discuss some possible rearrangements in the setting that may become necessary.

An Outline of the Pole Problem

In the forest are standing trees of various diameters and heights, some of which become poles. There are broad-leaved trees called hardwoods, and needle-leaved trees or conifers, called softwoods. In any tree there is a central core of more or less durable, nonliving wood known as the heartwood, which is surrounded by a layer of living wood known as the sapwood. There are durable woods in both the hardwood and softwood groups. The durable woods have relatively large heartwood cores and relatively thin sapwood; and the nondurable woods, on the other hand, are likely to have relatively thick sapwood.

The history of change in practice that has taken place since the first chestnut lines were built is a story of the shift from naturally durable woods to nondurable woods, and this change has involved the introduction and use of preservative materials to give the nondurable woods an economic service life.

One of the most important reasons for the shift was a national disaster in the form of the chestnut blight disease which has wiped out completely the commercial chestnut forests of the United States.

At the same time the supply of northern cedar became inadequate, at least as far as the sizes required for joint use were concerned. The question of selecting proper pole size became more and more important. The growth of the electrical industry had not followed a simple pattern, and the need for correlation of ideas and a rational standardization of pole practice led to the writing and acceptance of the American Standard Specifications for Wood Poles.[1] These specifications have had a most important influence on modern pole production. They were written around the natural poles of the forest; and they were designed to make suitable species interchangeable and to encourage the use of the greatest possible number of pole-size trees.

Other specifications had to be written or rewritten to cover the preservative treatment. The writers of these specifications focussed their attention on the finished pole, leaving only so much of process control in the specifications as was necessary to protect the timber from injury. This focussing generally took the form of defining acceptable limits for penetration. Practically speaking, the only universally accepted preservative was coal tar creosote. One major objective only was held in view in the preservation processes, namely, to fill the nondurable sapwood, whether it was very thin as in chestnut, or whether it made up 95 per cent of the pole as in many southern pines, with enough toxic creosote to prevent the growth of wood-destroying fungi.

Pole Use

Figure 1 is a copy of an old map made about the turn of the century to show the economic limits for the distribution within the Bell system of certain pole timbers. For the greater part of the country chestnut and northern cedar were favorites. In the western states western cedar was a natural selection. Creosoted southern pine had just begun to spread up from the southern pine forests, reaching as far north as St. Louis. The contrast between Figure 1 and Figure 2, which represents conditions in 1941, is striking. Chestnut is gone. The eastern scene is almost completely dominated by southern pine. Western cedar is distributed from the northwest and from the concentration yards at Minneapolis. Northern cedar is confined to parts of the New England states and the Lake states. Lodge-pole pine serves the Mountain states area. Douglas fir is being shipped from the fog-belt forests of Oregon and Washington, either directly or through Minneapolis to the eastern and southern states. The lines of distribution cross and recross in a perplexing pattern woven from preference, policy, price, and freight rates.

Many of the changes indicated in Figure 2 have been inevitable. Expansion of joint use, which has been easier since the standard pole specifications have become more widely accepted, has had a tendency to increase the length of the poles required for certain services. This has inevitably thrown the demand back on southern pine and western red cedar; and within the last two years the practical difficulties in securing large quantities of long southern-pine and western-cedar poles have practically forced the production and use of Douglas fir.

Figure 3 is a composite diagram, worked out by Doctor J. G. Segelken of the Bell Telephone Laboratories, presenting in a new fashion a picture of the relative use by the Bell system of some sizes of the pole species shown in Figure 2. Certain facts stand out plainly. One is the relative use of northern cedar and western cedar in the 30-, 35-, and 40-foot groups. Another is the major position of creosoted southern pine. A few years ago the Bell system bought between 30 and 50 per cent of all creosoted-pine poles produced in any one year. Now only about 15 per cent of the production goes into Bell system lines; but the predominance of creosoted southern pine in the Bell system use picture is a factor of major importance.

The Drain on the Forest

The degree of this importance is illustrated by Figure 4, derived from the annual reports prepared by R. K. Helphenstine, Jr.,[2] of the United States Forest Service. Piles and poles are much alike, and they come from the same forests, so that one cannot speak of a natural supply of trees for poles without taking the demand for piles into consideration.

The number of standing trees per acre in the pine forest that will make either piles or poles 35 feet or more in length is relatively small. Therefore, any increase in the demand for long poles inevitably means the working over of a larger num-

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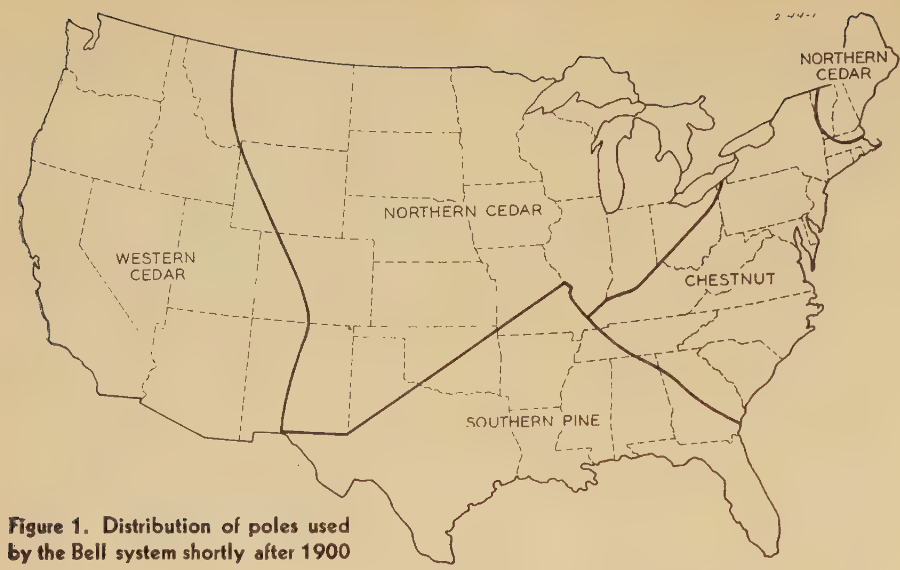

Figure 1. Distribution of poles used by the Bell system shortly after 1900

ber of acres with consequent increased labor and longer hauls. The Southern Forest Experiment Station at New Orleans conducted a unique survey in 1934–1936 which revealed that there was a more than adequate supply of pole trees of small diameter under 25 feet in length, but that the number of suitable trees in the larger sizes was relatively small. Figure 5 shows the relative use within the Bell system and outside of the Bell system for 35-, 40-, and 45-foot southern-pine poles, classes 1 to 7, American Standard. The 35-foot class 5 pole, which is a favorite for joint use makes up roughly 10 per cent of the total requirements. The relative natural occurrence in the forest of trees to make these poles is only one per cent of the total forest stand. The survey figures indicated that the number of trees suitable for 35-foot class 5 poles and for piles of about the same size taken together was approximately 3,500,000. This looks like an adequate supply. But approximately 23 per cent of the poles required for use outside of the Bell system are for 35-foot poles of smaller diameter, and the heavy demand for 30-foot class 6 and class 7 poles for cross-country lines and rural extensions during the last few years has meant cutting trees which, had they been left standing, might have furnished an entirely satisfactory supply of 35-foot poles at some later date.

The current requirements for 35- and 40-foot southern-pine poles are relatively much in excess of the occurrence of standing trees. The rapid expansion of the pulp and paper industry in the southern states and the heavy emergency demand for southern-pine lumber and piles have probably changed the relative proportions of the various sizes of standing trees since the survey. If the demand for creosoted-southern-pine poles 35 feet and longer were to continue at the present rate, a situation would soon develop in which these poles would be at a premium, and rigid straightness requirements would have to be relaxed in order to facilitate production. It would seem to be the better part of wisdom to use as many sizes as possible and to broaden the use of species to include western-cedar and Douglas-fir poles whenever it is practicable to do so.

Pole Manufacture and Standard Framing

Since the establishment of standard dimensions for the different species, there has been a definite trend toward greater mechanization in pole production. The most evident result of this trend is the pole-shaving machine for removing inner bark and smoothing the pole surface. Under Bell system specifications southern-pine poles are now trimmed by approved machine, and the trimming is limited so that the average size of the poles shall not fall below certain minimum circumferences at six feet from the butt, at mid-point, and at the top. The shaving machines are finding their way into the cedar industry, and western cedar is shaved to facilitate preservative treatment or to reduce the sapwood thickness so that it will not hold enough moisture to support decay. Machine trimming has improved the appearance of the southern-pine pole by removing the natural bumps at the knots, and by reducing the exudation of creosote, or "bleeding," from the pole surface. It also accelerates drying and facilitates production-line operation.

Generally speaking, the majority of the southern-pine poles manufactured for Bell system use are manufactured and framed to a standard pattern. At present the roof is cut square with the pole axis, and instead of the individual mortises for the crossarms, a slab gain is provided by machining a flat surface on the appropriate side of the upper part of the pole. The slab-gained pole is an economical all-purpose pole that takes the place of the mortise-gained pole, cable pole, guy stub, and push brace. There are, of course, cases in which special framing may be necessary or desirable; but special framing is a kind of custom job, and unless production orders are placed well in advance, it generally happens that specially framed poles must be produced on order.

The acceptance of standard framing has met with less resistance in southern pine than in western cedar. It is only within the last few years that the concept of a standard finished western-cedar pole has begun to take form. In the case of southern pine the principle of complete manufacture before treatment was established as a practically universal procedure before

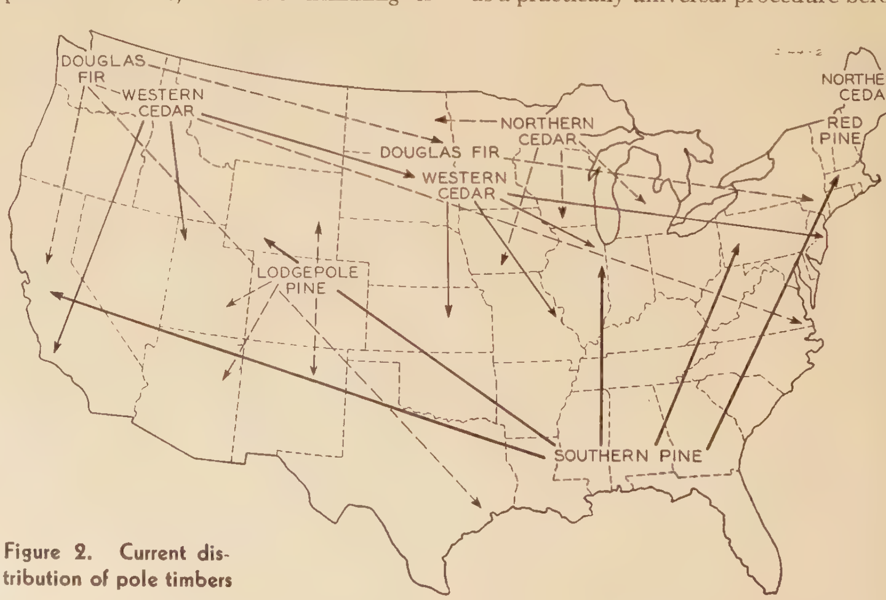

Figure 2. Current distribution of pole timbers

the creosoted-pine pole began to be accepted as a regular piece of line equipment, and the development of standard framing followed more or less naturally. In all cases gaining and boring the bolt holes is permitted after treatment in southern pine if the sapwood is 100 per cent penetrated.

Pole Preservation

The main purpose of pole preservation is to protect the nondurable sapwood from decay. The principles are relatively simple. A toxic substance is put into the wood in sufficient quantity to make the wood unpalatable to the wood-destroying fungi that cause decay. The addition of the toxic substance must be accomplished without material damage to the wood itself.

The preservative most in favor is coal tar creosote. The wood is impregnated by soaking the pole butts in open tanks or by applying pressure to the whole pole in closed cylinders. The butt-treating method has been applied with greatest success to the naturally durable thin sapwood species like chestnut and cedar, the aim being to put a protective layer of sapwood saturated with creosote around the naturally durable heart. In the case of cedar it is necessary to puncture the sapwood to secure satisfactory penetration. The open-tank method has also been used for the treatment of the thicker sapwood of lodge-pole pine, and it is now being employed as a supplementary process to the butts of poles that have been given a full-length treatment with a preservative salt.

Current treating specifications for full-length pressure treatment place the emphasis upon the product. Penetration is relatively more important than quantity of preservative retained. The Bell system, after some years of observation and experimentation, now specifies that the treatment of southern pine shall be by an empty-cell process, that the penetration shall be at least $2^1/_2$ inches unless 85 per cent of the sapwood is penetrated, and that the net retention shall average eight pounds of creosote per cubic foot of wood. Detailed discussion of the development of these requirements may be found in the *Proceedings* of the American Wood-Preservers' Association. The double requirement, based on both depth and per cent of sapwood penetrated, is necessary, because the sapwood thickness varies in poles of different lengths and circumferences.

A relatively low-residue creosote has been specified, defined by limiting the

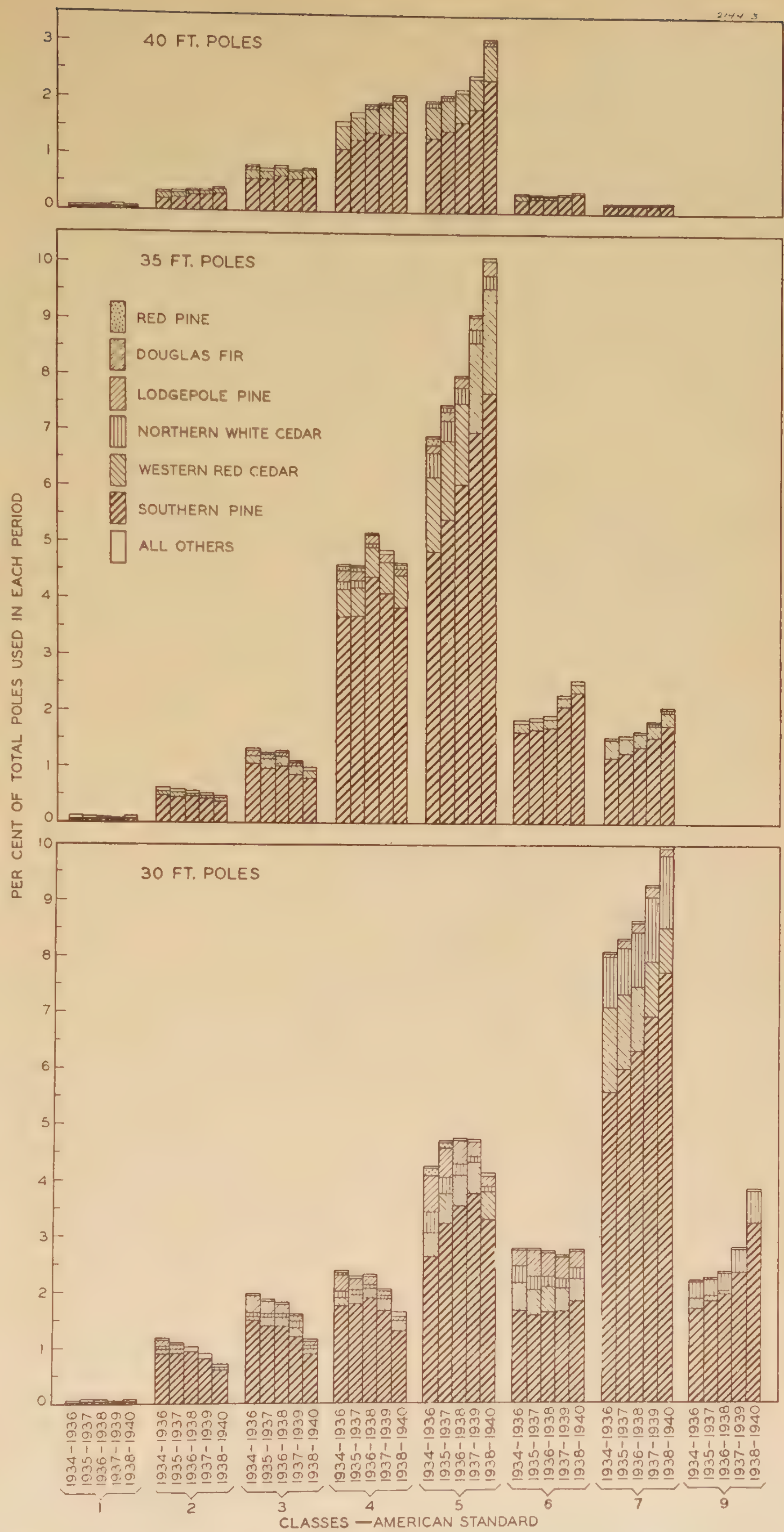

Figure 3. Relative use by the Bell system of 30-, 35-, and 40-foot poles, three-year moving averages

residue that remains after distillation has been carried to 355 degrees centigrade. Much of this low-residue creosote has been imported. At present such importation has practically ceased, and some adjustments are in order. The purpose of the low-residue creosote requirement was to make the product as acceptable as pos-

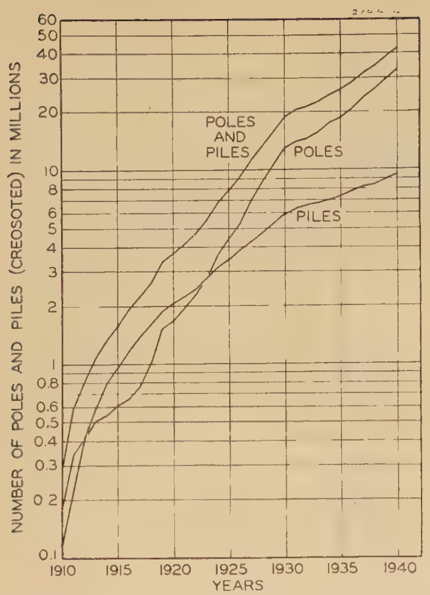

Figure 4. Creosoted-southern-pine poles and piles produced in the United States since 1910

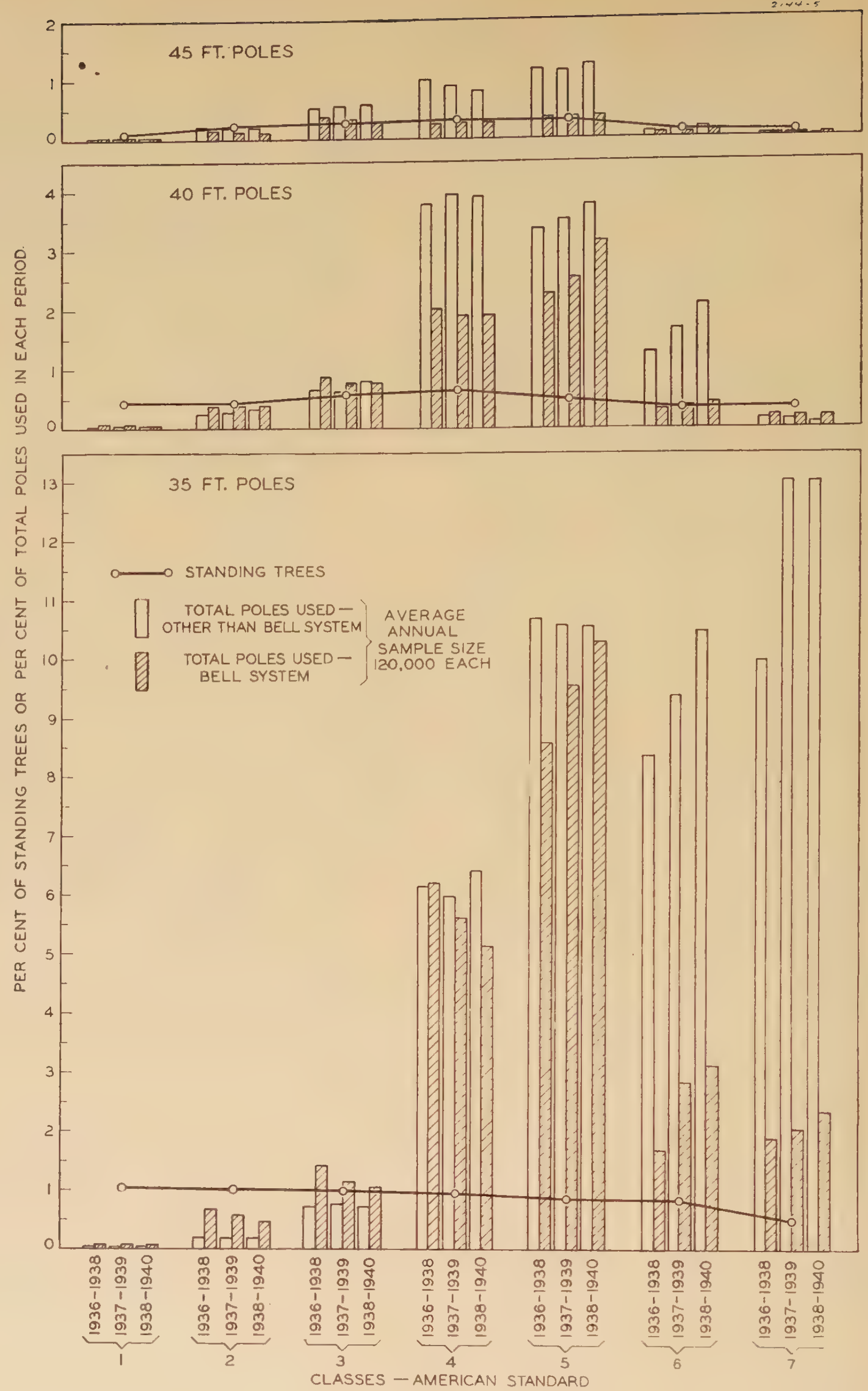

Figure 5. Forest supply and use in and outside the Bell system of 35-, 40-, and 45-foot southern-pine poles

sible to all parts of the Bell system and for all types of lines, whether urban or rural. At the time the requirement was written into the specification, the heavier residue creosotes available had a tendency to bleed badly, that is, to ooze from the poles after the poles were set in line, often for months or years. A black sticky pole is a dirty pole, and it makes trouble for the construction crews and the public alike. In adapting the creosoted-pine pole to urban use it has been found expedient to reduce the standard retention of creosote from 12 pounds per cubic foot to 8 pounds per cubic foot, to specify low-residue creosote and, in some cases, to require steam baths after treatment to clean the poles.

Full-length pressure treatment with creosote is also being applied to red pine, lodge-pole pine, western cedar and Douglas fir. In the case of Douglas fir the treatment usually takes several times as long as treatment of southern pine; and although the sapwood of Douglas fir is thinner, it is sometimes hard to treat as high a proportion of the sapwood as in southern pine.

In expanding the use of red pine, lodge-pole pine, and Douglas fir, it was necessary to focus attention on adequate penetration and to reduce the retention as far as practicable to prevent bleeding. In making compromises of this type one is forced to sidestep the main argument that has been so often advanced, that the reduction of retention from 12 pounds to 8 pounds in southern pine, for example, might mean inadequate service in line. The compromises appear to be working out fairly well, and, as a matter of fact, the actual concentration in the thinner sapwoods of Douglas fir and lodge-pole pine are at about the same level as the concentration in the sapwood of southern pine.

Penetration, Nonconformity, and Risk of Early Failure

Only in the case of southern pine has the evidence on the practicability of the penetration and retention requirements been fairly well established. The latest available information on southern-pine treatment by an eight-pound empty cell process is illustrated by Figure 6, which shows the smooth distribution of 17,556 poles, nonconforming for penetration, representing approximately 3.5 per cent out of a total sample of 525,000 produced in 1941. The distribution has been divided into nonconformity zones, numbered 1 to 5. The interpretation of the distribution is illustrated in Table I. From studies of the behavior of creosoted-southern-pine poles in line[3,4], certain failure factors have been determined which

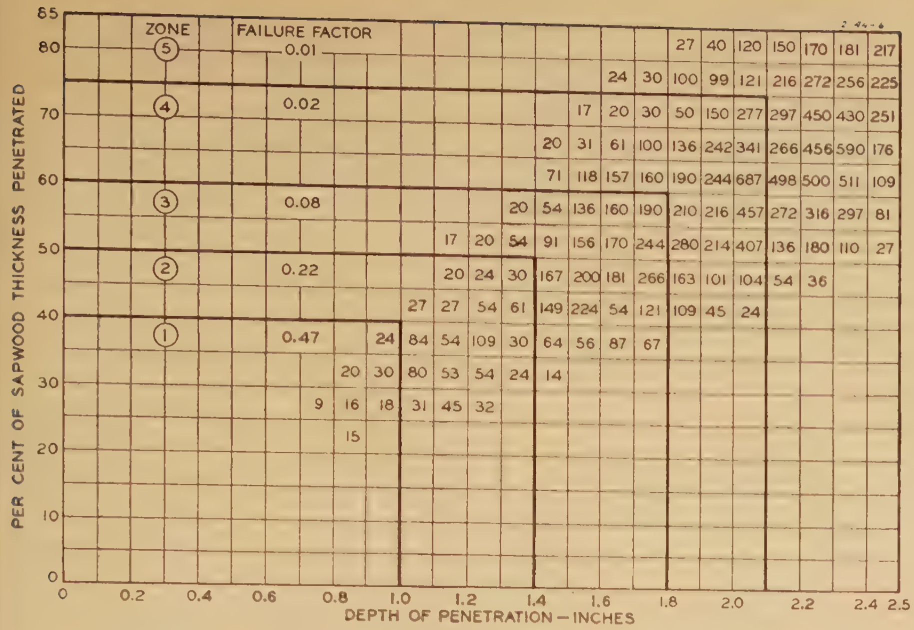

Figure 6. Distribution of southern-pine poles nonconforming for creosote penetration, 1941 production, arranged in nonconformity zones according to risk of early failure in line

are applicable to the percentage of poles in the respective nonconformity zones. These failure factors may be defined as the percentage of the poles within these zones which may be expected to fail within 15 years of installation. Recent experience indicates that these failure factors are probably high. It appears likely that the total risk of early failure in a group of poles like those under consideration is less than the estimated 0.128 per cent. The method of interpretation illustrated was first advanced several years ago, and up to the present no reason has been brought forward for changing it. Broadly, it means that poles which meet the penetration requirements of current standard specifications should have an entirely adequate service life.

Green Salt Treatment

A new preservative for this country, called green salt, a mixture of five parts potassium dichromate, three parts copper sulphate, and one part arsenic acid, in a cold water solution, has been used recently for the commercial treatment of seasoned southern-pine poles. The treating solution contains about seven per cent of the salts. The penetration has averaged about 98.5 per cent of the depth of the sapwood, which is better than the average generally obtained in creosote treatments; and the number of nonconforming poles was approximately three per cent. The distribution of nonconformity appears to be of the same order as that shown in Figure 6, with fewer poles in zones 1 and 2. The finished product is cleaner, lighter in weight, and stiffer than the creosoted pole, and the outer fibers are somewhat harder, making the green salt poles somewhat more difficult to climb than the creosoted pole. Generally speaking, the reaction of the field forces to the new poles has been excellent.

Immersion Treatments—Creosote, Petroleum, and Pentachlorphenol

The virtual shutting off of creosote importation by the war has stimulated the use of other types of preservatives such as mixtures of creosote and petroleum, with or without the addition of some other material such as pentachlorphenol; or the use of a five per cent solution of pentachlorphenol in petroleum alone.[5] The latter solution is being used for the full-length treatment of western cedar by an open-tank immersion process as well as for pressure treatment of southern pine.

Machine-shaved western cedar poles are being creosoted by immersion in hot creosote, followed by the usual butt treatment. The end in view in these western cedar treatments is to produce poles in which the hazard to linemen from sapwood decay will be eliminated.

Salt-Creosote Poles

During the last 15 years approximately 800,000 southern-pine poles treated with zinc meta arsenite have been placed in line. The majority of them have been purchased and set by power and light utilities. The deterioration that has occurred in these poles has happened at or below the ground line and, in most cases, the reduction in circumference at that point has taken place slowly. Above ground line the condition of the poles is excellent. To prevent the deterioration at the ground line, poles that are treated full length with zinc meta arsenite are now being given a butt treatment with creosote. Specifications for this double treatment call for a penetration in the above ground part of the pole of $2^1/_2$ inches unless 85 per cent of the sapwood is penetrated, as in the case of creosoted southern pine. Below ground a full sapwood penetration of creosote is not required. The aim of the specification is to provide for adequate protection of the butt without the use of too much creosote. This salt-creosote pole is being distributed in the northeastern states on the bases of strength and life expectancy equivalent to creosoted southern pine. Other salts than zinc meta arsenite, such as those covered by the current Federal specifications for use in treatment of timber not in contact with the ground, are under consideration. It is interesting to note in this connection that double treatment using zinc chloride followed by a creosote butt treatment was in use in Texas prior to 1910. Some of the poles of this vintage have given at least 34 years' service in line and are still good.

Conditioning Poles for Treatment

This brief discussion of preservation cannot be dropped without some mention of the problem of preparing poles for treatment. In theory at least, it would be desirable to dry poles of all species before treating them. Practically speaking, it is almost impossible, except at certain seasons of the year, to air-season southern-pine poles for either creosote or salt treatments without great risk of the poles becoming infected with molds and strength-reducing fungi during the process. The larger the pole, the harder it is to dry it. Charges of either green or dry poles may be treated satisfactorily with

Table I. Southern-Pine Poles—1941 Production—Estimate of Risk of Early Failure in Line

Nonconformity zone	Number of Poles	Per Cent of Total Poles	Failure Factor	Risk of Early Failure Per Cent of Total Poles
1	132	0.025	0.47	0.012
2	839	0.160	0.22	0.035
3	2,862	0.545	0.08	0.044
4	5,432	1.035	0.02	0.021
5	8,291	1.579	0.01	0.016
	17,556	3.344		0.128

due care in the control of the treating process. Partially seasoned poles, either alone or in mixture with seasoned or green poles, may make the treating results very variable. The Bell system specification requires steam conditioning for at least six hours at 259 degrees Fahrenheit before impregnation with creosote, in order to smooth out some of the variables and to sterilize the wood.

To prevent deterioration during seasoning, some producers follow the practice of pretreating all pole stocks as soon as they have dried down to the point where such treatment can be given economically with approximately four pounds of creosote per cubic foot. The poles are then stacked in the yard and held for orders. The additional poundage required to meet the minimum retention specified is added by a second treatment. In other cases the poles are treated under pressure with weak solutions of preservative salts.

The kiln-drying of southern-pine poles[6] has been carried far enough experimentally during the last few years to show that it is a distinctly promising method of preparing the poles for treatment. Ironically enough, it begins to look as if the creosoted kiln-dried poles would have to be steamed for a short period at the end of the creosoting process in order to clean them and prevent subsequent bleeding of the creosote from the pole surface.

In the case of certain of the salt preservatives organic matter in the green or partially seasoned pole causes a very rapid chemical reduction of the preservative solution if either the wood or the solution is heated; and this reduction does not occur to the same degree if the wood is dried before treatment, and the solution is applied cold. In any event a dry pole treats quickly, and the plant is able to save cylinder hours, and a pole that has once been dried and then given either a creosote or a salt treatment dries out subsequently more rapidly than a pole that was not seasoned before treatment.

Electric Resistance

It is extremely important to bear in mind the practical difficulties involved in seasoning and treating timber before entering on a discussion of the conductivity of the treated wood. Measurements made by improved methods[7] confirm the generally accepted conclusion that the electric resistance varies inversely as the moisture content. There is a temperature factor, but it can be more or less ignored if the measurements are made within certain temperature limits. It is not so easy to control moisture content. The moisture may come from two sources, that is, it may be the moisture that is left in the sap that was in the tree when it was felled, and it may be moisture absorbed from rain or melting snow as the pole is lying in a concentration yard or standing in line.

The sapwood of a green pole may contain more water than its own dry weight.

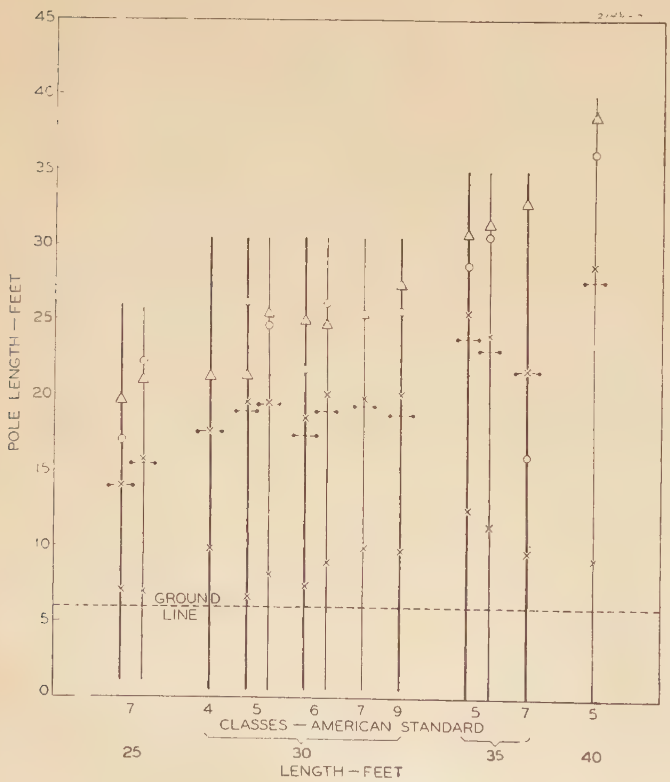

Figure 7. Independence of maximum knots and points of break in southern-pine poles and pole-top sections

Δ Maximum group of knots in any 12-inch section

o Maximum single knot

•--• Ground line for test of top section

x Point of break

—Creosoted poles

—Green salt poles

When only Δ is shown, the maximum single knot is in the maximum group

The upper x shows location of point of break in top section

At a moisture content equal to 27–30 per cent of its dry weight, wood is said to be at the fiber saturation point, that is, at a point where there is no free water in the wood cells. If the moisture content is above this fiber saturation point, the electric resistance of the wood is low. Below the fiber saturation point electric resistance increases sharply as the moisture content decreases.

Suppose that, either as the result of well-considered seasoning practice on the part of the producer, or on the insistence on the part of a possible customer, a group of relatively high-resistance poles has been produced and shipped. It would be quite wrong to assume that these poles would all have high resistance at the time they were placed in line and equipped for service. During shipment moisture may get into the open seasoning checks and be absorbed by the inner sapwood and untreated heartwood of the pole. Or the poles may be exposed to rain and snow for months before they are erected. In fact, any method of open flat storage previous to line use is almost certain to bring about a great variation in the resistance of poles that may have started out on a relatively high resistance level. Even the best of storage conditions, under cover or in vertical piles, is no guarantee of high resistance of the pole in line. Driving rains, sleet, or snow storms may wet the windward side of the pole and bring it to practical saturation during the course of a storm. The character of the preservative itself simply adds another variable to these general conditions over which very little control can be exercised.

Aside from drying a pole properly before treatment so that it will dry out more quickly after treatment, there is little that can be done in writing specifications, or in actually producing poles, that will make it possible to turn out high resistance units; and after the poles have once been produced, each stick becomes an independent variable in its resistance characteristics. In practical operation, therefore, no pole can have a high resistance all the time.

Pole Strength

In writing the American Standard Specifications for Wood Poles,[1] all of the species were dimensioned so that they would have approximately equal resisting moments at the ground-line for any given class and length. These strength requirements have been translated into what may be called standard breaking loads starting at 1,200 pounds for class 7 and running to 4,500 pounds

Table II. Southern-Pine Poles—Breaking Test Data

No.	American Standard Class	American Standard Length	Average Diameters in Inches: Maximum Single Knots	Average Diameters in Inches: Maximum Knot Groups	Whole Pole Relative Average: Breaking Load*	Whole Pole Relative Average: Modulus of Rupture**	Top Section Relative Average: Breaking Load*	Top Section Relative Average: Modulus of Rupture**
Creosoted Poles								
1	4	30	1.2	1.8	122	119	178	117
4	5	30	1.7	3.8	118	103	156	101
3	5	35	1.1	3.9	150	130	192	118
5	6	30	1.6	5.0	112	99	127	80
5	7	25	1.5	4.9	116	104	140	96
1	7	35	1.9	5.2	127	124	175	133
3	9	30	1.6	4.8	†	120	†	140
22			1.5	4.5	122	110	152	105
Green Salt Poles								
3	5	30	2.2	4.1	150	129	205	107
8	5	35	1.6	3.5	146	127	182	104
2	5	40	1.4	2.7	138	130	223	119
38	6	30	1.7	4.5	121	104	151	99
11	7	25	1.2	3.7	149	135	189	119
8	7	30	1.5	3.9	148	129	170	94
70			1.6	4.1	133	116	167	103
92			1.6	4.2	100§	100§	125	90

* The standard breaking loads were taken as 100 in each case. These standard loads are: class 4—2,400 pounds; class 5—1,900 pounds; class 6—1,500 pounds, and class 7—1,200 pounds.

** The standard ultimate fiber stress for creosoted-southern-pine poles—7,400 pounds per square inch—was taken as 100 in each case.

† No standard breaking load for class 9 has been established. For the three poles listed the average breaking loads for the poles and for the pole-top sections were 1,317 pounds and 1,840 pounds, respectively.

§ The averages for the whole poles were taken as 100 for comparison with the top sections.

for class 1, approximately on a 25 per cent geometric progression.

Three years ago R. C. Eggleston of the Bell Telephone Laboratories began a series of breaking tests on machine-shaved southern pine treated with creosote, and with green salt. A novel scheme was introduced. The top sections of the poles as well as the poles as a whole, have been tested whenever possible. The results of part of these tests, on 92 poles and pole-top sections, are summarized in Table II. The poles averaged well above their rated class breaking loads, above the American Standard fiber stress—7,400 pounds per square inch—and within the expected variation for modulus of rupture. The modulus of rupture of the pole-top sections averaged 90 per cent of the modulus of rupture of the poles as a whole. The top sections of the poles were broken with short lever arms and, in spite of lower modulus of rupture, were on the average still sufficiently strong to meet their specified pole-class breaking load.

The position of the maximum knot and the maximum-knot group in relation to the point of break for the poles as a whole and for the pole-top sections are shown in Figure 7. Each line in the figure represents one of the groups defined by columns 1, 2, and 3 in Table II, and within given class length groups the creosoted-pole and the green-salt-pole diagrams are placed side by side to facilitate comparison. The independence of maximum knot position and break position is obvious. Only the pole-top sections, which had to be clamped in the holding device to represent the point of guying, broke at conspicuous knot groups. In some instances the very first point of break seemed to be at small knots an inch or less in diameter or even in hidden knots which did not appear on the surface of the poles at all. Occasionally poles will break during unloading. Apparently, such breakage is the result of impact shock far in excess of what the poles could stand. Generally, this impact shock comes from dropping the pole or from catching it between others as it is rolled off the car or trailer. The breaking in such cases at knot groups is coincidental with the shock and point of application and does not indicate dangerous weakness in the pole itself.

Ground-Line Treatment

One other subject remains for brief mention, namely the prolongation of the life of the existing pole plant. The evidence from test plot experiments and observation of poles in service indicates that some sort of ground-line treatment should be applied to untreated poles that are worth saving at the time of inspection, as a part of the regular inspection procedure. When one looks back over the years, it appears that the mistake, if any, that was made in earlier ground-line treatments was not in the treatment itself, but rather in the assumption that a single application would be effective for the life of the pole. The methods now recommended are the Osmose process or the simplified procedure that consists of the regular inspection and excavation, and treatment with a water-soluble salt and creosote. Originally a pound of sodium fluoride was dumped into the excavation around the pole, and the hole lightly backfilled. Then, after making a narrow V-shape trench against the pole, about one gallon of cold creosote was poured into the hole by letting it run against the pole, at a height of about 12 inches above the ground line, from the flattened spout of a watering can. The backfill was then finished, leaving the earth around the ground line fairly saturated with creosote and leaving a water-soluble salt to be taken up by the moisture in the wood. Sodium fluoride has been replaced in part by borax in the general plan as a result of the shortage of the former salt. The protection afforded by such a treatment seems to be good for about five years.

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Interim Report on Guides for Overloading Transformers and Voltage Regulators

AIEE COMMITTEE ON ELECTRICAL MACHINERY
Transformer Subcommittee*

THE present war emergency has crystallized the need for additional information concerning the maximum load capability of modern-design transformers and voltage regulators in order that these apparatus may be utilized to a greater extent for meeting present and new load requirements with a minimum amount of new equipment, thus conserving critical war materials. It will be noted that the papers[1–3] presented at the AIEE convention June 24, 1942 give somewhat divergent views on permissible overloads and on the effect of temperature on transformer insulation.

The relation between the life expectancy of insulation as indicated by laboratory tests and the actual life of a transformer is largely theoretical, so that the use of such information must be tempered by sound judgment based on operating experience. This report represents a compromise of views that is considered conservative and satisfactory for immediate general use, subject to the limitations and cautions given herein.

Consideration is given to operation with normal life expectancy and also with moderate sacrifice in life expectancy, thus taking into account the effect of the following factors:

(*a*). Characteristics and limitations of the apparatus involved.

(*b*). Ambient temperature.

(*c*). Load factor.

(*d*). Supplemental cooling.

(*e*). Sacrifice of life expectancy.

Normal Life Expectancy

In this report normal life expectancy is based on continuous operation at rated load with a daily average ambient temperature of 30 degrees centigrade for self-cooled or forced-air-cooled transformers, 25 degrees centigrade for water-cooled transformers, and other usual service conditions as given in American Standard *C*-57, Section 2.000. The "Guides for Operation of Transformers" included in American Standard *C*-57 give general recommendations for the loading of oil-immersed transformers and voltage regulators for recurrent and emergency overloads with small effect on normal life expectancy, while this report will supply, in addition to other information, data for heavier overloads with moderate sacrifice of life expectancy.

Loading for Normal Life Expectancy

The maximum load capability is determined to a considerable extent by the type and the electrical characteristics of the specific piece of apparatus. However, some general guides may be set up, based upon the more usual comparatively modern design characteristics, and with provision for taking into account the factors of temperature of the cooling medium, load factor, thermal ability of bushings and accessories, expansion of oil due to temperature increase, possible pressure in sealed transformers, method of cooling, and so forth.

Design Characteristics

The general information on transformer loading in this report applies to the comparatively modern-design transformers built since about 1928. The information will not apply in the usual case to the older-design apparatus without consideration of the design characteristics of each piece of apparatus.

Ambient Temperature

For low ambient temperatures the continuous kilovolt-ampere loading may be increased one per cent for each degree that the daily average temperature is below 30 degrees centigrade for self-cooled transformers, three fourths of one per cent for each degree below 30 degrees centigrade for forced-air-cooled transformers, and one per cent for each degree below 25 degrees centigrade for water-cooled transformers.[4] Similarly, the loading should be decreased two per cent for each degree that the daily average ambient is above these temperatures. For continuous loading these rules result in normal life expectancy, the same as for operation at rated load with ambient temperatures of 30 degrees centigrade and 25 degrees centigrade respectively. Detailed applications of these rules are given in the American Standard *C*-57, "Guides for Operation of Transformers."

Under some conditions greater permissible overload capability can be obtained by using "equivalent annual ambient" instead of the daily average ambient when applying the rule for overloads due to change in ambient. The equivalent annual ambient is the temperature which, if maintained constantly, would result in the same aging as that occurring under the actual ambient temperature throughout the year. This matter is being given further consideration.

Load Factor

For daily load factors below 100 per cent the loading may be increased 0.3 per cent for each per cent that the daily load factor is below 100 per cent,[5] with normal life expectancy. More nearly accurate corrections based on an actual load curve are not usually justified for normal loading. In no case should the overload permitted by this factor exceed 15 per cent, corresponding to 50 per cent daily load factor.

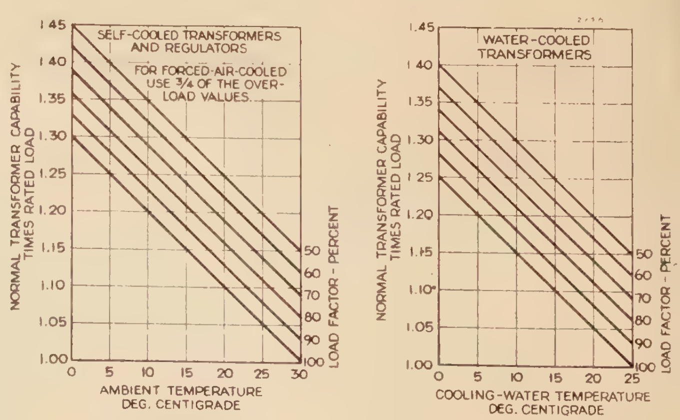

Figure 1. Transformer and regulator capabilities for normal life expectancy

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* Personnel of AIEE transformer subcommittee: M. S. Oldacre, chairman; F. S. Brown, E. S. Bundy, J. E. Clem, I. W. Gross, V. M. Montsinger, J. R. North, W. C. Sealey, F. J. Vogel, C. F. Wagner.

Acknowledgment is made of the assistance given by R. T. Henry, H. W. Hartzell, J. S. Lennox, and D. L. Levine.

Temperature Rise of Windings

The information in this report is based on apparatus designed for the standard 55 degrees centigrade rise. If a transformer has been designed for other than 55 degrees centigrade rise, a rating to give 55 degrees centigrade rise can be calculated, and then the correction factors stated herein can be applied.

Supplemental Cooling—Existing Transformers

The load that can be carried on existing transformers can be increased by adding auxiliary cooling equipment such as radiator fans, external forced oil coolers, or water spray equipment. The amount of additional loading that such devices permit varies widely depending on

(*a*). Design characteristics of the transformer.

(*b*). Type of cooling equipment.

(*c*). Permissible increase in voltage regulation.

(*d*). Limitations of associated equipment.

Specific data should be obtained for each individual transformer.

When applying supplemental cooling to existing transformers, the permissible overloads should be based on the hottest-spot temperature rise instead of on average winding temperature rise. Usually the hottest-spot temperature rise over top oil temperature increases as the 1.6 power of the load.

If it is found that the bushings, leads, or other accessories are not a limiting feature, oil-insulated self-cooled transformers by the addition of fans may have the output increased in many cases 25 per cent and in some cases up to 33 per cent. Similarly, the addition of external forced oil cooling to oil-insulated self-cooled or oil-insulated water-cooled transformers may increase the output in many cases 25 per cent and in some cases up to possibly $66^2/_3$ per cent.

Water-spray equipment may be used in some cases as an emergency method of additional cooling. It is not generally considered satisfactory as a means of obtaining continuous additional capacity or for year-round operation. The added capacity obtainable by the use of water spray on oil-insulated self-cooled transformers is dependent on the air and water temperature, humidity, design of bushings, leads, and other accessories of the transformer. The water spray equipment may increase the capacity in many cases 25 per cent and in some cases up to possibly 60 per cent.

Emergency Loading With Moderate Sacrifice of Life Expectancy

The foregoing discussions have dealt with loading of transformers on the basis of normal life expectancies. In this section loading with moderate sacrifice of life expectancy during infrequent emergencies to values above those given in the American Standard *C*-57 "Guides for Operations of Transformers" is discussed.

On the basis of available data it is reasonable to consider that hottest-spot temperatures for durations shown in Table I represent an average sacrifice of life with each such emergency operation of not more than one per cent of the normal life expectancy as determined by the tensile strength of the winding insulation.

Table I. Temperatures for Short-Time Emergency Loads With Moderate Loss of Life of Insulation

Duration of Load—Hours	Hottest-Spot Temperature—Degrees Centigrade
1	137
2	130
4	125
8	120
24	110

With the above temperature limits and where specific data on the individual transformer are not available, the following loads are considered satisfactory for modern transformers and regulators with average ambient temperature during the overload period of 30 degrees centigrade for self-cooled and forced-air-cooled transformers and a water temperature of 25 degrees centigrade for water-cooled transformers. These overload values apply to the normal continuous name-plate kilovolt-ampere rating for output voltages equal to or above name-plate rating, and

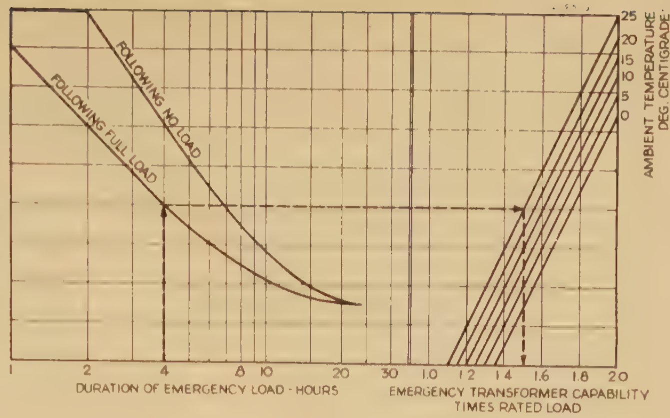


Figure 3. Emergency load capability of water-cooled transformers larger than 500 kva

See text for cautions and limitations

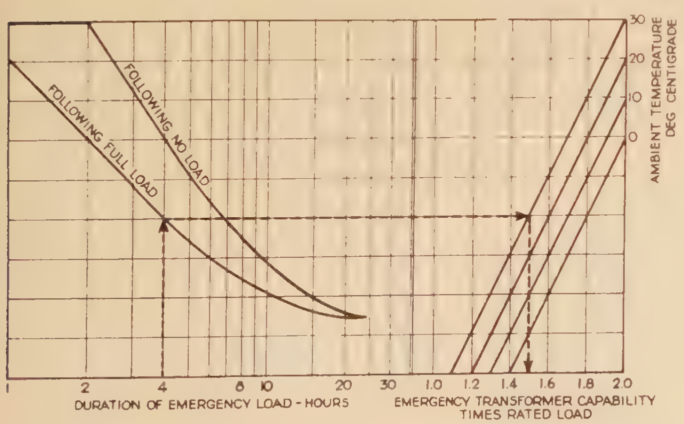


Figure 2. Emergency load capability of self-cooled transformers larger than 500 kva

See text for cautions and limitations

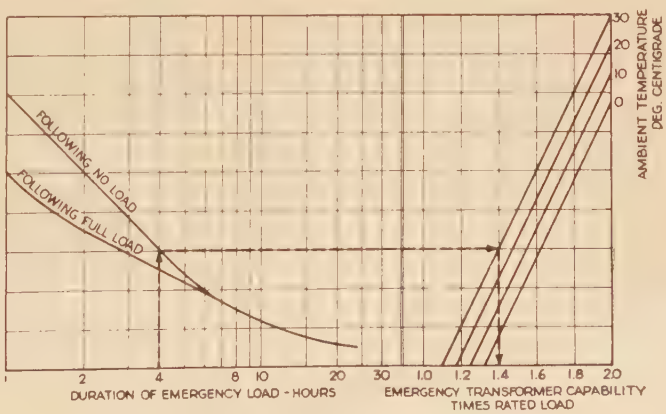


Figure 4. Emergency load capability of forced air-cooled transformers larger than 500 kva

See text for cautions and limitations

Table II. Short-Time Emergency Overloads for Transformers Rated Above 500 Kva*

(See text for cautions and limitations regarding ambients, rating, number of overloads, and so forth)

(Times Rated Load)

Duration of Load—Hours	Self-Cooled and Water-Cooled: Following Full Load	Self-Cooled and Water-Cooled: Following No Load**	Forced-Air-Cooled: Following Full Load	Forced-Air-Cooled: Following No Load**
1	1.9	2.0†	1.6	1.8
2	1.7	2.0	1.45	1.6
4	1.5	1.7	1.35	1.4
8	1.35	1.45	1.25	1.25
24	1.25	1.25	1.15	1.15

* Special consideration should be given to installations involving exceptional heavy-current designs.

** Values for operation "Following No Load" are on the basis that the transformer has been excited for at least several hours' time prior to application of the load. The loads that can be carried following partial load can be determined with sufficient accuracy by direct interpolation.

† It is considered that two times rated load is the maximum that should be carried, regardless of time, without special consideration.

Table III. Short-Time Emergency Overloads for Transformers Rated 500 Kva and Below,* and for Regulators

(See text for cautions and limitations regarding ambients, rating, number of overloads, and so forth)

(Times Rated Load)

Duration of Load—Hours	Transformers 500 Kva and Below: Following Full Load	Transformers 500 Kva and Below: Following No Load**	Regulators: Following Full Load	Regulators: Following No Load**
1	1.7	1.9	1.5‡	1.5‡
2	1.55	1.7	1.5	1.5‡
4	1.4	1.5	1.4	1.5
8	1.3	1.35	1.3	1.35
24	1.2	1.2	1.2	1.2

* Special consideration should be given to installations involving exceptional heavy-current designs.

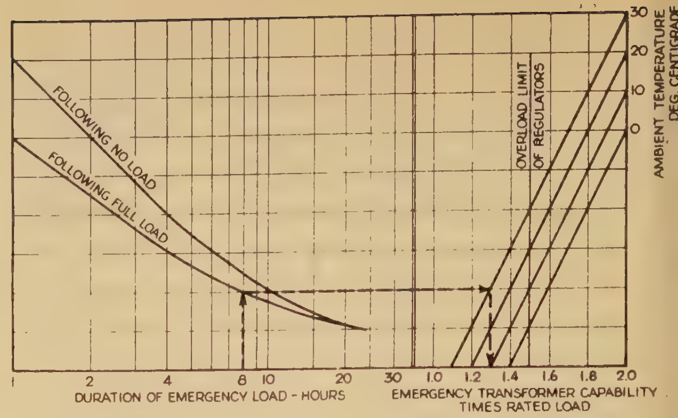
** Values for operation "Following No Load" are on the basis that the transformer has been excited for at least several hours' time prior to application of the load. The loads that can be carried following partial load can be determined with sufficient accuracy by direct interpolation.

‡ It is considered that 1.5 times rated load is the maximum that should be carried, regardless of time, without special consideration.

to normal continuous rated amperes for output voltages below name-plate rating. It is assumed that the emergencies will occur not more than once on any one day and not more than 25 or 30 times during

Figure 5. Emergency load capability of self-cooled transformers (500 kva or less) and of regulators

See text for cautions and limitations



the normal life of the transformer, and that the insulation, windings and oil are reasonably clean and free from excessive amounts of moisture and sludge.

Table II shows permissible loads for infrequent emergencies for modern sealed-type transformers rated above 500 kva which have the oil effectively protected from exposure to air and moisture. Table III shows permissible loads for infrequent emergencies for modern transformers rated 500 kva and below and for modern regulators.

Conclusions

Information has been presented herein concerning the effects of the characteristics of specific apparatus, ambient temperature, load factor, supplemental cooling, and so forth, in determining the load capability of transformers and voltage regulators for both normal and emergency conditions.

The effects of these factors are cumulative, as follows:

For *normal operation* with normal life expectancy the effects of load factor, ambient temperature, and supplemental cooling may all be added in determining the load capability. Figure 1 illustrates the application of factors for ambient temperature and low load factor in determining the normal capability for transformers and regulators.

For *short-time emergency operation* with moderate sacrifice of life the effect of the actual ambient temperature during the emergency and of supplemental cooling should be considered in addition to the values given in Tables II and III. Figures 2 to 5 inclusive illustrate the application of these values to determine emergency load capabilities.

The principles and facilities described in this report may be applied to existing or proposed new equipment and should be effective in conserving critical war materials.

Caution: It must be recognized that overloads should not be applied to transformers or regulators without a thorough study of the various limitations involved. Among these limitations are oil expansion, pressure in sealed-type units, bushings, leads, soldered connections, tap-changers, and so forth, and the thermal capability of associated equipment, such as cables, reactors, circuit breakers, disconnecting switches, current transformers, and so forth. These may constitute the practical limit in load-carrying ability.

Before overloading transformers or regulators to the full extent covered in this report, it is recommended that the overload capabilities of the equipment be checked with the manufacturer.

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A Compressed-Air Operating Mechanism for Oil Circuit Breakers

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General Field of Application

THE recent trend toward better use of existing lines and power sources has brought a demand for faster operation of oil circuit breakers and further simplification of control. In the past it has been common practice to use a solenoid or motor to provide closing energy for these breakers, either of which requires a substantial battery, means for keeping it up to full charge at all times, large conductors between the battery and the breaker to handle the heavy currents which exist for approximately one second while the breaker is closing, and generously proportioned control equipment. Consequently, the cost of apparatus associated with the breaker, but actually installed apart from the breaker, becomes a significant item.

Compressed air can be used as a source of energy to close these breakers, and with several obvious advantages. In the first place, the electrical drain on a battery at the instant of closing is only the control relay current, not the full solenoid closing coil current. This may be a reduction of the order of 1 to 100 which is reflected in smaller batteries and chargers, lighter control cables, and contactors. Another advantage is that full air pressure is instantly available to start closing the breaker, whereas a solenoid builds up in power at a slower rate, depending on the inductance of the coil. Thus it is possible to secure faster breaker operations.

In a fundamental way the apparatus required for closing a breaker with compressed air consists of:

(*a*). A source of air comprising a compressor and reservoir.

(*b*). A cylinder and piston within which the air can be released to force the breaker to the closed position.

(*c*). Suitable means for controlling the flow of air by means of magnetically operated valves operated from a distant point.

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Obviously, it is not practical to build the commercial apparatus in quite such simple form, and various requirements of operation have made it desirable to add refinements as the following will disclose.

The Air Supply

It is desirable that an air reservoir be located close to each operating cylinder, as air flowing a long distance through small pipes will lose pressure to such a degree that the delivered air may not be able to accomplish the work of closing the the breaker in the required time. Hence, a 20- or 30-gallon reservoir is installed in the same housing with the operating mechanism and control and mounted on the end of the breaker.

For reliable service it has been recommended that a motor-driven compressor be mounted in each housing, thus making each breaker entirely independent of any other. It could be argued that this is an unnecessary expense where several breakers are installed in close proximity to each other, and where one compressor could perhaps handle the several reservoirs. However, it appears that most operating men look at the possible dollar saving as overbalanced by the hazard of shutting down the entire group of breakers, in case of the failure of the one compressor, and the possible difficulty of maintaining interconnecting air piping clear and tight.

If it is preferred to use a single compressor unit for an entire station, it is recommended that a double compressor be used so as to provide increased reliability. Also, proper consideration must be given to protection against mechanical damage to piping and to the drainage of any water which might condense in the air line. If freezing temperatures will be encountered in the locality of installation, adequate sizes of pipe and sumps must be provided to prevent blocking with ice.

The assembly of compressor and reservoir is built to exceed the standards required for industrial systems and railway brake installations, in which continuity of performance has been an important factor worked out years ago. It is designed to meet all usual safety code inspection requirements, and the reservoir carries the stamp of the boiler code inspector of The American Society of Mechanical Engineers. An automatic governor maintains a maximum pressure of 200 pounds per square inch, starting the compressor whenever the pressure drops to 15 pounds below the preset maximum. A safety valve limits the reservoir pressure to 225 pounds. Ordinarily only one-half or three-fourth horsepower is required in the compressor motor so that little current is drawn from the a-c supply line. In ordinary service the motor will run only a few minutes each day, as required for the occasional breaker operation and in order to overcome slight leakage loss. Starting with an empty reservoir, a full charge can be pumped in one-half hour. The motor need not run each time the breaker operates, as the amount of air used is frequently so small as not to drop the reservoir pressure to the point where the automatic governor picks up. In case of the failure of the supply voltage to the compressor motor, a full reservoir contains enough air to operate the breaker at least five times. Further, if there should be no immediate demand for an operation, it would require more than 12 hours for a normally tight air system to leak down to a dangerously low pressure, giving a wide margin of time to re-establish compressor power or service. A low-pressure switch indicates through an alarm circuit when the drop in reservoir pressure is becoming acute, and another switch locks out the closing relay circuit so as to prevent the attempt to close, when the air pressure is too low to insure completion of the operation.

The Lever System

Figure 1 shows a typical mechanism assembly mounted on the tank of a 138-kv outdoor oil circuit breaker. The compressor and reservoir occupy the space at the right of the assembly. At the left and center is the operating mechanism itself, with a magnetically operated inlet valve conveniently located directly in front of the main cylinder. The current demand of this magnet valve is well under two amperes.

One requirement for a mechanism of this type is that it shall be capable of reclosing the breaker promptly, under some conditions in not over 20 cycles (one-third second). This mechanism meets the demand by retaining a positive mechanical connection between the breaker and the operating piston at all times, so that at any time it may be desired air admitted to the cylinder will immediately act to close the breaker, regardless of

whether it may have reached the full open position or not. A single lever, hinged at one end, and carrying a roller at the other end to be held under the hook shaped latch when the breaker is closed, is attached to both the piston and the breaker pull rod at the center.

The breaker is closed under the effect of the compressed air and latched in the closed position, so that the air can be cut off at the end of this stroke. When the breaker is called upon to open, the latch is released by a trip magnet, and the usual accelerating spring on the breaker furnishes the impetus needed to secure the proper contact opening speed. The opening is therefore independent of the condition of the air supply, and required interrupting time of the breaker is controlled entirely by the mechanical time of unlatching the mechanism and accelerating the contacts by springs which were preloaded during the closing stroke of the breaker.

It is evident that when a direct mechanical connection exists between the breaker and the piston, closing and reclosing operations are easily obtained. Experience with solenoids has shown that it is necessary to provide them with mechanically trip-free linkages so as to permit full opening speed without the contacts being delayed by the magnetic drag of the closing solenoid. The mass of the solenoid core also acts as a drag to prevent full opening speed. These difficulties are not present in the air mechanism when a light piston is used, and when some means is provided for dumping from the operating cylinder, the air still under pressure, so that it will not restrict the opening speed of the contacts.[1] The latter demand is met by a series of exhaust ports—on the side wall of the main cylinder, near the fully closed position—which are opened simultaneously with the operation of the breaker trip coil. The total area of these ports is approximately four square inches, and oscillographic pressure records show that the collapse of pressure is so fast that less than 25 pounds remains in the cylinder at the time the contacts part. The extreme speed of this dump valve is secured by utilizing the high-pressure air on the end of the cylindrical valve, after it has once been cracked by a pilot piston or small magnet.

While the single-lever design adequately meets the requirements of quick reclosing, some modification is necessary where manual operation of the smaller breakers is concerned. In this class of apparatus, one man can complete the closing operation with a single quick stroke of the removable handle, but must be protected against holding the breaker in on a fault, as would be the case with the non-trip free mechanism. A solution to this problem is shown in Figure 2, where the single-lever system for power operation is modified when it is to be manually operated, by unlocking the fixed hinge point and temporarily restraining the automatic latch over its roller, so that, if necessary, the breaker can be tripped in the manner of the conventional double-lever system. Thus, the benefits of trip-free action can be secured, while in the same parts the inherently fast reclosing possibilities of the non-trip free action are retained.

Controlling the Air Flow

There is a characteristic mechanical load curve for all breakers having the general shape of that marked "Breaker Load" in Figure 3. It is made up of the dead load, balance and accelerating springs, and the extra load of contact pressure springs at the closed position. The breaker toggles may alter the magnitude of these combined loads at different parts of the stroke, but the fundamental characteristic remains. The simple solenoid pull curve is well adapted to this, since it develops a greater pull as the cores come together (see curve marked "Solenoid Mechanism" also in Figure 3). The surplus of power shown for the solenoid

Figure 1. Type CAS-8 mechanism for outdoor service

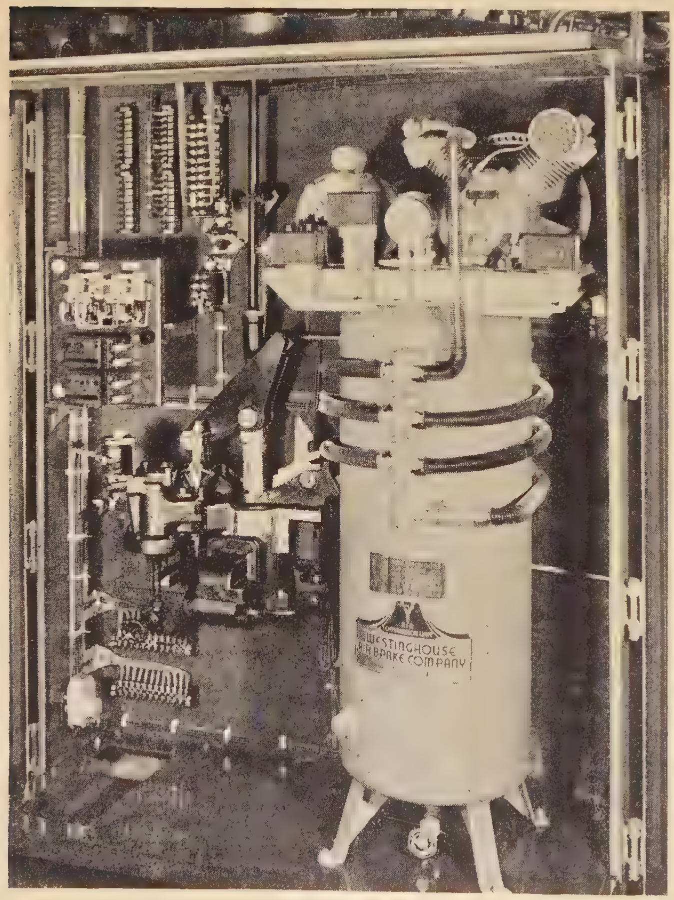
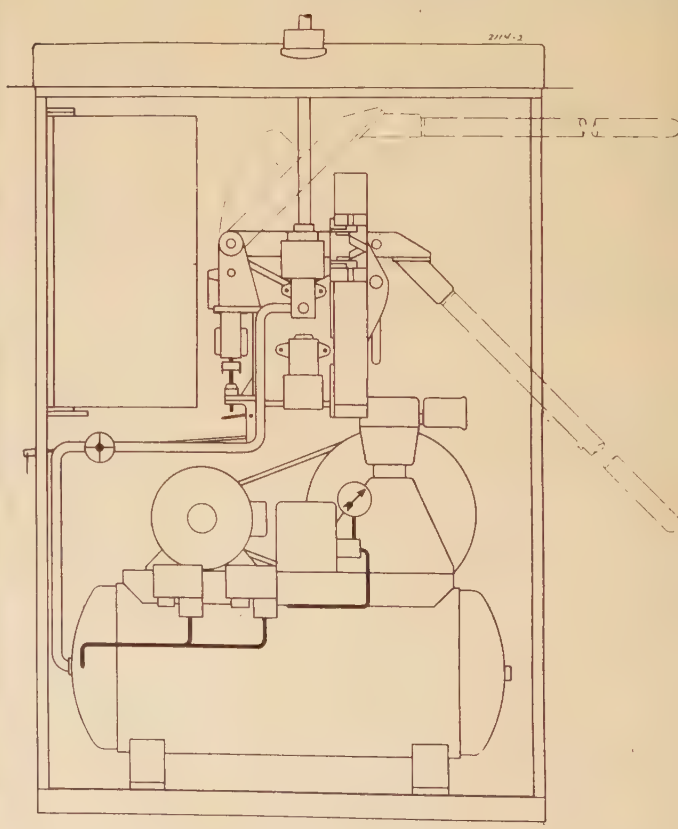


Figure 2. An outdoor-type compressed-air mechanism with trip-free hand-closing lever



goes into speed and, of course, is eventually lost, as breaker strikes its stops in the closed position, but the general shape of the pull curve of the solenoid usually falls just above the load curve of the breaker, so that there are no large instantaneous surpluses or deficiencies. With the same breaker and a simple air-operated mechanism, "Elementary," the pull characteristic would be largely flat, drooping slightly toward the closed position, as the air was expanded into the operating cylinder or dropped in pressure, because of resistance to flow in the connecting passages between the reservoir and the cylinder. Rather than change the breaker levers to co-ordinate the simple air-mechanism pull and the conventional breaker load, a scheme has been worked out to throttle or control the flow of air, and the air-mechanism pull curve takes the general shape of that shown in Figure 3, "Air Mechanism—Controlled."

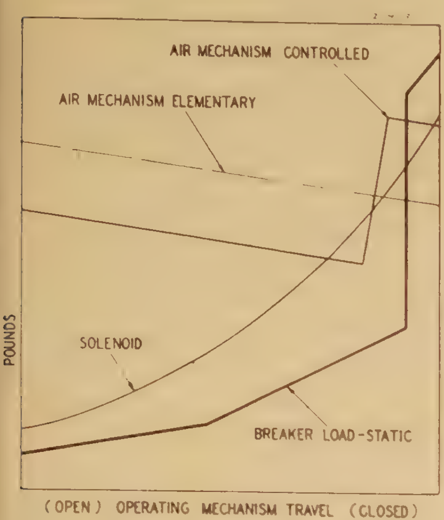
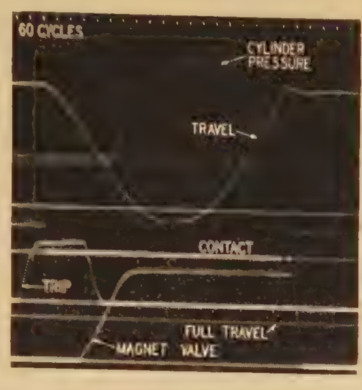


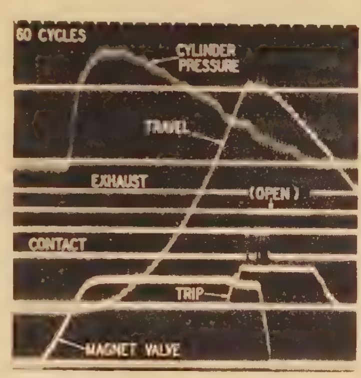
Figure 3. Typical curve of a breaker load and mechanism pull

The principle of the throttle is that when the load is light the air passage is kept small. When the load is about to rise, the throttle is opened by a mechanical linkage, and with the augmented air supply, the stroke is completed with just the right amount of pull to insure positive latching and yet not with excessive slam. Since the throttle is adjustable both as to amount of opening and to position at which it becomes effective, it makes possible the use of one size of mechanism on several types and ratings of breaker.

The throttle is operated through a slotted link, since it is found that full air pressure is required over a greater part of the stroke when fast reclosure is attempted, as compared to that required to complete a simple closing and latching. Under this condition of fast reclosure greater power must be available, first to arrest the opening motion, and then to bring the contacts back to the closed position. The use of a throttled air system of course means the choice of an air pressure and piston area such that the required maximum pull will be secured, but this does not mean that it takes more air than an unthrottled system—rather, that through control, the power available is released as demanded by the load, with proper provision made for those operations particularly requiring high speed.



(a). Reclosing in less than 20 cycles



(b). A close-open operation

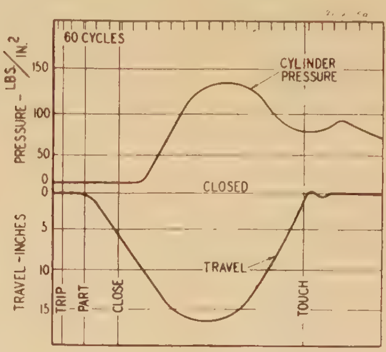
Figure 4. Oscillograms of breaker timing

Result of Tests

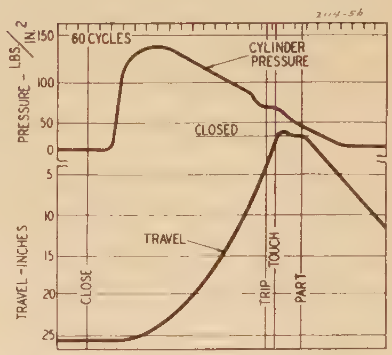
A timing test setup was made on a typical 138-kv oil circuit breaker equipped with an eight-inch compressed-air mechanism of the type described above. Oscillographic records, shown in Figures 4a and 4b show the variation in cylinder pressure, the motion, or travel, of the movable contacts, the motion of the exhaust valve, a battery indication of contact engagement, and the trip-coil and magnet-valve-coil currents. For the sake of clarity the essential data have been traced from these oscillograms and are shown in Figures 5a and 5b.

Figure 5a shows an *OC* operation for which the time from energizing the trip coil to retouching the contact was 19.3 cycles. It will be noted that the magnet valve was energized $4^1/_2$ cycles after the trip coil, and that the contacts had traveled approximately 36 per cent of the entire stroke at the instant the air valve was opened. The contacts continue opening with reducing speed until there is a cushioned reversal of motion at 57 per cent of the full stroke. The cylinder pressure falls as the contacts travel toward the closed position, due to expansion and throttling. The result is a quick reclosure without excessive strain on the mechanism or pole unit parts, and without excessive slam as the breaker reaches the closed position.

Figure 5b illustrates a *CO* operation using the same breaker and mechanism.



(a). Reclosing in less than 20 cycles



(b). A close-open operation

Figure 5. Clarified speed and pressure records from Figure 4

This test was set up for pretripping, as soon as the auxiliary switch contacts in the trip circuit were made up. Even under this condition the "air-trip-free" action of the mechanism is demonstrated by the fact that there is no back pressure on the operating piston after the first few cycles of travel. A comparison of the opening portion of Figures 5a and 5b shows that at the end of eight cycles after energizing the trip coil, the contact separations exceed that required to interrupt normal short-circuit currents.

Field service throughout a full range of seasonal changes in weather verifies the

Current- and Potential-Transformer Standardization

AIEE COMMITTEE ON PROTECTIVE DEVICES
Current Transformer Subcommittee*

Synopsis: This report has been written to summarize the work of the AIEE current transformer subcommittee and to discuss the considerations which led to the adoption of the material in the revision of section 4, "Instrument Transformers," of the Proposed American Standard for Transformers, *C*-57. Special attention is given to the principles underlying the establishment of the overcurrent requirements of current transformers for relaying service and the revision of the method of specifying the accuracy of instrument transformers for metering service. Mention is also made of the reasons for making other changes, such as in the preferred primary current ratings, potential-transformer ratios, and so forth.

DURING the past two years the current transformer subcommittee of the relay subcommittee revised the section on instrument transformers of the American Standard, *C*-57. When this group was appointed, its assignment was to formulate specifications for current-transformer performance in the overcurrent range for relay application. During the progress of the work, however, the activities of the group were enlarged until they covered the complete revision of section 4 of the Proposed Standard for Transformers. This was the result of a desire to correlate the new material with the old material and to save time and duplication of effort by having the work done by one group. Contacts however were made with the other interested groups so that the work proceeded smoothly with the support and co-operation of these groups. This paper has been written to present the reasons why the material in section 4 is there in its present form.*

Primary Current Rating of Current Transformers (4.021)

The original proposal in the printed draft of *C*-57 contains 29 values of primary current ratings divided into four groups. It was generally felt that this represented an excessive number of ratings and that the number of primary current ratings could be drastically reduced and still adequately cover the field.

The first step was to set up a list of ratings based on one of the sequences in the system of preferred numbers. This gave a list of approximately 16 ratings which covered the range adequately. However, no agreement could be reached upon the values obtained by this simplified method of approach, because certain active ratings were not included and, accordingly, a different method of approach had to be used.

Primary current ratings of 10, 25, 50, 100, 200, 400, and 800 had been established by the joint AEIC-EEI metering group of the association of Edison Illuminating Companies and the Edison Electric Institute, and these ratings were used as a nucleus around which the list now appearing in the standards was established. It was necessary to effect a compromise among the requirements of the groups interested in applying relays, meters, and instruments. Because of the whole-hearted co-operation among the interested groups in reaching a solution which would afford a saving to the industry, the number of preferred primary ratings has been substantially reduced, the reduction being from 29 to 21 ratings.

The preferred primary current ratings are now in one table in paragraph 4.021. It is not the intent that all the values be made available for each circuit voltage; rather, it is the intent that the ratings used for any circuit voltage be limited to as few of values appearing in the table as possible.

It was also felt that there were too many preferred ratings for double-ratio current transformers, and the reduction in the number of values required was a simple matter after the problem had been solved for single-ratio current transformers.

Standard Burdens for Rating Purposes (4.030)

Section 4.030 of the revised standard concerns standard burdens for current transformers, combining and taking the place of the former two sections 4.030 and 4.031 dealing respectively with "Burdens for Current Transformers for Metering Service" and "Burdens for Current Transformers for Relay Service."

Formerly, burdens designated as *X*, *Y*, and *Z*, or 2.5 and 15 volt-amperes at 0.9 power factor, and 50 volt-amperes at 0.5 power factor, respectively, at five amperes and 60 cycles were set as standard metering burdens; then a separate series was set up for relay service as 12.5, 25, 50, 100, and 200 volt-amperes at 0.5 power factor at five amperes and 60 cycles, these burdens to be used for rating purposes.

These two groups have now been combined into one list of standard burdens designated as *B*-0.1, *B*-0.2, *B*-0.5, *B*-1, *B*-2, *B*-4, and *B*-8 corresponding to the ohmic values at 60 cycles of the burden of 0.1 ohm, 0.5 ohm, and so forth. The first three have a burden power factor of 0.9, and the last four have a power factor of 0.5 at 60 cycles. The old meter burden *Y* of 0.6 ohm at 0.9 power factor and the relay burden 0.5 at 0.5 power factor have been replaced by a single burden *B* 0.5, corresponding to 0.5 ohm at 0.9 power factor or 12.5 volt-amperes at 0.9 power factor. There is no essential difference in these three values as far as performance is concerned, but the new value fits into the list of burdens in more orderly fashion.

It was also found necessary to add an additional value of burden; namely, that

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* Numbers following subheads refer to paragraphs of the currently proposed revision of section 4 of the Proposed American Standard for Transformers *C*-57 which are given in appendix 3 of this paper.

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high degree of reliability obtained in laboratory testing. The flexibility in design and the compactness of the unit obtained by the use of compressed air, and the encouragement gotten from commercial contacts, indicate an increasing demand for this type of mechanism.

References

1. A New 15-Kv Pneumatic Circuit Interrupter, L. R. Ludwig, H. L. Rawlins, B. P. Baker. AIEE Transactions, volume 59, 1940, September section, page 528.

A 2,500,000 Kva Compressed-Air Powerhouse Breaker, L. R. Ludwig, H. M. Wilcox, B. P. Baker. AIEE Transactions, volume 61, 1942, May section, pages 235–41.

designated as B-0.2, or five volt-amperes at 0.9 power factor for metering service. This burden is now required for certain current transformers used on low-voltage circuits.

In the revised table then, burdens B-0.1, B-0.2, B-0.5, and B-2 are standard burdens for metering service for rating purposes, and burdens B-0.5, B-1, B-2, B-4, and B-8 are standard burdens for relay service for rating purposes. It should be noted that these burdens also may be designated by the volt-amperes and power factor at five amperes and 60 cycles of each, if the longer designation is preferred. The new designations are different from the old ones in order to avoid confusion with former designations. The proposed values are as shown in paragraph 4.030.

Burdens for Potential Transformers (4.036)

Former section 4.032 concerned standard burdens for potential transformers for rating purposes in all classes of service. This is now included in the revised standard in 4.036. Only one change was made; namely, the volt-amperes of burden W was changed from 13 to 12.5 merely so that it would be one half of the value of the X burden, which is 25.

Classification of Current Transformers for Accuracy Metering Service (4.031)

Former section 4.033 concerned standard accuracies of current transformers and section 4.035 concerned standard accuracies of potential transformers for metering service. In the revised standard these are included as sections 4.031 for current transformers and 4.037 for potential transformers. Accuracy classes have been specified on a different basis from that previously used. Formerly, fixed limits were assigned separately to ratio error and phase angle for each accuracy class, but on this basis the class designation did not in itself indicate the probable effect of the transformer on the accuracy of measurement of watts or watt-hours.

Since the primary concern with respect to ratio and phase angle of an instrument transformer is the effect on the accuracy of measurement of watts or watt-hours, it is more consistent to use this effect as a criterion of performance, rather than merely the arbitrary ratio error and phase-angle limits which might be chosen. A measure of this effect is the ratio of what a wattmeter would read in a circuit with an instrument transformer having no ratio error or phase angle to what it would read in the same circuit with an instrument transformer having the usual errors. This ratio has been called "transformer correction factor" or TCF, and the standard defines it as "the factor by which the watt-hour meter registration or the wattmeter indication must be multiplied to correct for the error introduced by the transformer through the combined effect of the ratio correction factor (RCF), phase angle (β),* and power factor angle (θ) of the metered load.

Thus for a current transformer (see appendix 1):

$$\text{TCF} = \text{RCF}\,\frac{\cos\theta}{\cos(\theta-\beta)}$$

At the same time it was also decided to use RCF instead of per cent ratio error.

$$\text{RCF} = \frac{\text{true ratio}}{\text{marked ratio}}$$

The values of TCF for a transformer are dependent upon the power factor of the circuit being measured and the RCF and phase angle of the transformer. Where the transformer angle is small, the relation between these various factors can be expressed in a rather simple manner within accuracy limits sufficiently good for the purpose. By choosing limits for θ and TCF, the limiting values of phase angle and RCF are set. The range of power-factor angle chosen is from 0 to 53 degrees 08 minutes, corresponding to circuit power factor of 1.0 and 0.6 lagging respectively, which covers the usual range of power factors encountered in practice. For these conditions the relation between the limiting values becomes:

For CTs, $\beta = 2{,}600\,(\text{RCF} - \text{TCF})$, in minutes

For PTs, $\gamma = 2{,}600\,(\text{TCF} - \text{RCF})$, in minutes

If e represents the permissible per-unit error in the measurement, it may be positive or negative, and the corresponding limiting values of TCF will be $(1 - e)$ and $(1 + e)$. See Figure 1.

The limiting values of TCF as set up in the standard have been chosen entirely from the practical viewpoint of transformer accuracies which are required and those attainable with transformers either of modern or earlier design. The new accuracy classes and, consequently, the limiting values of TCF correspond very closely to the former accuracy classes at circuit power factors at or near 1.0. A new class of wider limits than before has been added to permit an accuracy rating for current transformers with ratio errors in the order of two per cent at 100 per cent rated current such as for certain bushing and similar types.

It will be noted on investigation, for example, that the former 1/4 class results in the limiting TCFs for current transformers at 100 per cent rated current of 0.99365 and 1.00635 in the range of line power factors from 0.6 to 1.0, which produces maximum meter errors of about ±0.635 per cent. This is more than double the 0.25 per cent error sometimes assumed to be indicated by the former 1/4 class designation, which corresponded to the maximum ratio error only without consideration of phase angle and over-all metering error.

For the new 3/10 class at 100 per cent rated current β varies from +15.6 to −15.6 minutes (see Figure 1), the limits of which are 50 per cent greater than formerly for the 1/4 class but which result in only half the maximum possible meter error for that class. It should be noted that this method is the equivalent of that used for the former class M only which has been continued as the class 5/10.

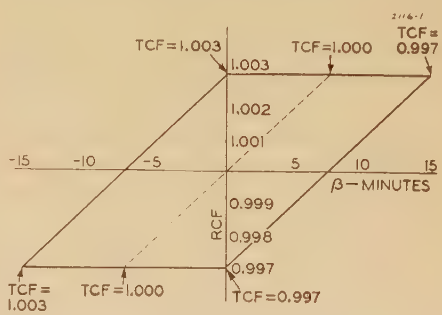


Figure 1. Parallelogram showing limits of ratio correction factor, RCF, and phase angle, β, for a current transformer having a 3/10 accuracy class rating

There accordingly results a means of accuracy rating of instrument transformers for metering service that is of more significance than formerly to the average meterman, since the accuracy class rating is associated directly with the effect of transformer characteristics on the accuracy of measurement. The limits set appear to be practical and adequate for all ordinary purposes, and the method utilized is not involved but provides the necessary rigidity in a simple manner for specification purposes.

Standard Accuracy Classes for Current Transformers for Relaying Service (4.032)

This section, not previously included in the proposed American Standard for

* β is defined as the angle by which the secondary current leads the primary current.

Transformers is the result of an intensive effort to establish standard classes of overcurrent performance. It provides a much needed benchmark for comparison of the performance of various current transformers and for specifying their accuracy at the high currents involved in protective-relay applications. It also establishes in a general way the limits within which a given transformer does not exceed specified errors under overcurrent conditions.

It is important to note that the terms "accuracy" and "performance" as covered by this standard refer to steady-state conditions only. The performance of current transformers under transient conditions is far too complicated to permit of standardization at the present time. While the generalization may be made that the better the steady-state performance, the better the performance will be under transients, nevertheless, it must be realized that adequate performance within the limits of these standards is not necessarily indicative of adequate performance under transient conditions. Reference should be made to the various items of the bibliography.

Current transformers are classified primarily on the basis of the standard burden which each can carry with a specified secondary current without exceeding a specified per cent error. To standardize ratings five amperes has been selected as the standard rated secondary current, and for overcurrent performance the transformers are classified primarily on the basis of their ability at secondary currents of the same value, namely 20 times rated.

Since all the transformers are standardized on the secondary current, the current need not be part of the necessary designation. The designation proposed to identify or express the accuracy classification is, for example, 2.5*H*400 or 10*L*-200. The first number identifies the accuracy class and the limiting per cent error. The letter identifies the type of accuracy classification. The second number identifies the terminal voltage which can be developed on one of the standard burdens at 20 times rated secondary current without exceeding the specified per cent error. This scheme of designation was adopted, because it was expected to give the most useful information.

In the application of relays it is essential that the operating engineer have a knowledge of the performance of the current transformer under overcurrent conditions. While considerable error can frequently be tolerated, it is essential to know the amount of the error or a limiting value which will not be exceeded. This point of view led to the establishment of two accuracy classes for current transformers for relaying service.

In general, the change in the phase angle need not be considered in relaying applications because often it is of no consequence and is relatively small as long as the ratio error is not excessive. Therefore, most attention is focused upon the ratio error. The standard limiting ratio errors are 10 per cent and $2^1/_2$ per cent, the particular one applicable being indicated by the first figure in the accuracy designation.

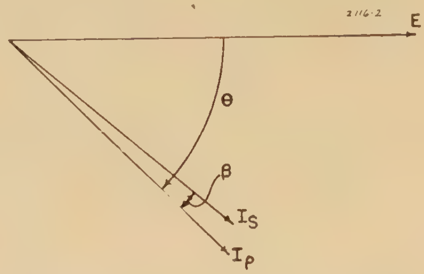


Figure 2. Actual and indicated currents

The figure of 10 per cent has been chosen, both because errors not in excess of this figure can be disregarded in most relay applications, and because for errors in excess of this figure the accuracy of most current transformers falls off very rapidly. The second accuracy class with $2^1/_2$ per cent as the limit in ratio error has been set up to cover those cases where 10 per cent cannot be tolerated. The error is expressed in per cent of the secondary current.

The overcurrent requirements for current transformers are based upon an overcurrent of 20 times rated in the secondary winding. With a five-ampere rated secondary current, the choice of 20 times rated secondary current is preferable to 20 times rated primary current, because the former results in a secondary current of 100 amperes, which is a convenient number for such purposes.

While the current transformers are classified primarily on the basis of the burden which can be carried at 20 times rated with the rated accuracy, it is desirable to specify, if possible, the burden which can be carried at lesser overcurrents than equal accuracy. An analysis showed that this could be done very simply for transformers of relatively high internal impedance,* such as wound types. Transformers having relatively low internal impedance can be standardized at only one point. Therefore, any method conceived of standardizing the performance at reduced overcurrents for the transformers of relatively low internal impedance, such as bushing type, had serious disadvantages. It was either too complicated or, if simple, necessitated in many cases transformers much larger than would be required to meet only the accuracy requirement at the basic standardizing point of 20 times rated.

In order to capitalize on the ability to standardize the accuracy of the high-internal-impedance types over the range of 5 to 20 times rated secondary current, without penalizing the relatively low-internal-impedance types, two separate classifications have been set up, designated as *H* and *L*. The letter *H* or *L* indicates whether the transformer is classified as a "high-internal-impedance" or a "low-internal-impedance" type. The reason for this distinction is as follows:

The excitation voltage upon which exciting current and consequent ratio error depend is the sum of the secondary terminal voltage plus the internal-impedance drop caused by the secondary current. If the secondary current is reduced from 20 times rated to 5 times rated, but the burden increased inversely, so as to keep the secondary terminal voltage constant, the excitation voltage will decrease due to the reduced internal-impedance drop. The exciting current decreases quite rapidly with a decrease in excitation voltage and for the high-internal-impedance-drop types this effect is great enough that the per cent error tends to remain constant. Thus, for the designs of high-internal-impedance drop, the requirement can be imposed that rated accuracy must be maintained for currents from 5 to 20 times rated with such burdens as to give a terminal voltage no greater than that obtained at 20 times rated current with rated burden. In only relatively few cases will this further requirement increase the size.

Two examples will clarify the meaning of the accuracy designations.

1. A 10*L*400 current transformer is of low internal impedance (as a bushing type). It can carry a *B*-4 burden from one to 20 times rated secondary current, or 5 to 100 amperes, with not over 10 per cent error. It can induce 400 volts with not over ten amperes exciting current.

2. A 2.5*H*200 current transformer is of high internal impedance (as a wound type) capable of supplying a *B*-2 burden at from one to 20 times rated secondary current with not over 2.5 per cent error. It can also supply any standard burden which will not require over 200 volts with secondary currents from 5 to 20 times rated, without exceeding 2.5 per cent error. At 2.5 am-

* The term "internal impedance" is used to identify that elusive characteristic of current transformers through which the exciting current required for a specified secondary induced voltage becomes different for different values of secondary current.

peres on the open-circuit saturation curve the transformer will induce over 200 volts.

Application Data for Current Transformers (4.033)

There is a growing appreciation of the fact that where applicable the saturation-curve method of calculating the performance of current transformers presents a distinct advantage over other methods involving the use of ratio and phase-angle curves. Transformers of the low-internal-secondary-impedance type are susceptible to such treatment. Accordingly, it was thought desirable to establish in the standard application data which shall be available from the manufacturers for use in this connection.

These data should include curves showing the excitation current as a function of the excitation voltage, or volts per turn, together with sufficient data to determine the inphase (watt) component and out-of-phase (magnetizing) component, or the phase angle. The data should also include the resistance of the secondary winding and such information in reference to the internal impedance as may be usable.

Calculations for transformers having low internal impedance may be rather simply made. The initial step is to determine required excitation voltage as based upon the secondary current and the sum of the burden ohms and the secondary resistance.

The exciting current is the maximum error current and may be read directly from the curve, and usually this is all that is required. If the phase angle or a more precise ratio error is required, it may be determined by one of the methods discussed in some of the references in the bibliography. All of these methods use the exciting current in magnitude and position with respect to the excitation voltage, although the mechanics may be different, and they are all essentially a solution of the vector diagram, Figure 3.

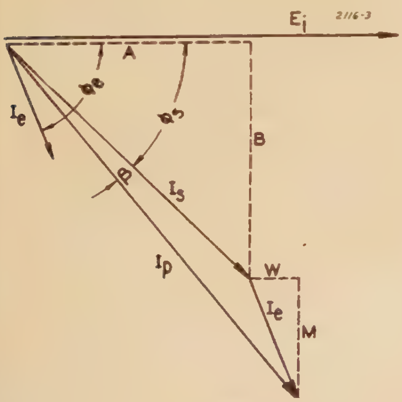


Figure 3. Vector diagram of transformer—low internal secondary impedance type

A few of the methods are given in appendix 2, and it is hoped that experience will indicate a preference for one or the other of these methods.

It is very unfortunate that transformers of the high-internal-impedance type are not directly susceptible to the simple method of calculation, although a somewhat similar method has been proposed. The Standards, therefore, continue the use of the familiar ratio and phase-angle curves for expressing the performance of this type of transformer.

Mechanical Limit for Current Transformers (4.046)

Because the mechanical strength specifications (paragraph 3.050, American Standard *C*-57) of power and distribution transformers is stated in a manner which involves a time element ranging from two to five seconds, it was suggested that the mechanical limit for current transformers should also involve a time element, and one second was suggested. However, the situation is quite different for the two types of transformers; the power or distribution transformer limits the short-circuit current to a definite value, while a current transformer has no appreciable effect in limiting the fault current. Actually, the time element for power and distribution transformers has more concern with the thermal requirement than with the mechanical. If the transformer does not fail within the first few cycles of a fault it will not fail at all mechanically.

There is some opinion that a current transformer may fail mechanically after the crest of the first maximum half cycle has passed, and this has apparently been confirmed to some extent by tests. Also it was quite strongly felt that one second was entirely too long a time in view of the fairly rapid decrement in the fault current. To facilitate testing the current is specified on a symmetrical basis, and it is permitted to cover the time range in successive applications. The proposed requirements are shown in paragraph 4.046.

This paragraph as written includes material which properly belongs in the test code. Due to the lack of a test code at the present time, this material is placed in the standard, because it involves the demonstration of the specifications of mechanical limit. The rated mechanical limit is specified in rms symmetrical amperes, coupled with the stipulation that the transformer must withstand the forces produced by this value of current when the current wave is completely offset. In testing this may be demonstrated by a symmetrical wave of twice the value of the rated mechanical limit maintained for the specified time of six cycles; or it may be demonstrated by successive applications of current with no intentional delay between applications, in which case the crest current may be measured for the final significant cycle of each application and must not be less than twice the crest corresponding to the crest of the rated mechanical limit.

Open Secondary Circuit of Current Transformers (4.047)

The original *C*-57 simply required that current transformers operate with the secondary circuit open "without damage to the transformer." In many cases the strict interpretation of this rule resulted in an abnormal design without any compensating features. Accordingly it was thought better to correlate the requirement for this condition with the test on the secondary windings. The proposed requirements are shown in paragraph 4.047.

Preferred Ratios for Potential Transformers (4.020) and Standard Insulation Classes and Tests for Potential Transformers (4.050)—Table IX

The question of what insulation test was standard for a potential transformer of a given ratio for circuits below 15 kv was the cause of many misunderstandings. The solution of this problem consisted of combining the tabulation of preferred ratios with the tabulation of insulation classes and tests.

When the preparation of *C*-57 was first begun, it was recognized that certain instrument transformers had impulse levels one step lower than those being considered for the circuit voltages with which the transformers were being used, although they could withstand the proposed low-frequency test required for these same circuit voltages. The circuit voltages in question were in the 5.0-, 8.7-, and 15-kv classes, and to take care of this situation note *c*, reading as follows, was proposed:

"(*c*) Indoor instrument transformers are available in the 2.5-, 5-, 8.66-, and 15-kv classes insulated one level lower than the values of this tabulation."

This note was the cause of great confusion, because its wording permitted misinterpretation, and in the printed draft of *C*-57 it was interpreted as applying to the lower-frequency test also. It resulted in a line of potential transformers having ratios 20:1, 40:1, and 60:1,

which had impulse insulation levels corresponding to the present insulation classes 2.5, 5.0, and 8.66, respectively. These transformers, according to the present standard, cannot however be connected in wye for use between line and neutral at their rated excitation voltage, because they are not insulated for this service. Because of this, there has been considerable effort to eliminate them entirely from the standard. However, these transformers represent existing practice and are used extensively, particularly for metering applications. Placing them in the table and associating their ratios with their proper insulation class permits their continued use and eliminates the possibility of confusing them with the trans-

Preferred Ratios Standard Insulation Classes and Dielectric Tests for Potential Transformers

Table IX of Proposed Standard C-57

Insulation Class (Name-Plate Rating) Col. 1[a]	Preferred Ratio Col. 2[b]	Rated Voltage: Secondary Volts Col. 3[b]	Rated Voltage: Primary Winding (Name-Plate Rating) Col. 4	Normal Circuit Voltage on Which Transformers May Be Used: When Δ Connected Volts Col. 5[a,c]	Normal Circuit Voltage on Which Transformers May Be Used: When Y-Connected Volts Col. 6[a,d,e]	Dielectric Tests: Low Frequency: Kv RMS Col. 7[f]	Dielectric Tests: Impulse: Chopped Wave: Kv Crest Col. 8[f]	Dielectric Tests: Impulse: Chopped Wave: Minimum Time to Flashover μsec. Col. 9[f]	Dielectric Tests: Impulse: Full Wave: Kv Crest Col. 10
1.2	1–1	120	120	120	208	10	36	1.0	30
	2–1	120	240	240	416	10	36	1.0	30
	4–1	120	480	480	832	10	36	1.0	30
	5–1	120	600	600	1,040	10	36	1.0	30
2.5	20–1	120	2,400	2,400	2,400	15	54	1.25	45
5.0	20–1	120	2,400/4,160 *Y*	2,400	4,160	19	69	1.5	60
	40–1	120	4,800	4,800	4,800	19	69	1.5	60
8.66	35–1	120	4,200/7,280 *Y*	4,200	7,200	26	88	1.6	75
	40–1	120	4,800/8,320 *Y*	4,800	8,320	26	88	1.6	75
	60–1	120	7,200	7,200	7,200	26	88	1.6	75
15*L*	60–1	120	7,200/12,480 *Y*	7,200	12,000	34	110	1.8	95
	70–1	120	8,400/14,560 *Y*	8,400	14,400	34	110	1.8	95
	100–1	120	12,000	12,000	12,000	34	110	1.8	95
	120–1	120	14,400	14,400	14,400	34	110	1.8	95
15*H*	60–1	120	7,200/12,480 *Y*	7,200	12,000	34	130	2.0	110
	70–1	120	8,400/14,560 *Y*	8,400	14,400	34	130	2.0	110
	100–1	120	12,000	12,000	12,000	34	130	2.0	110
	120–1	120	14,400	14,400	14,400	34	130	2.0	110
25	200/115–1	69.3/120	13,800/24,000 *Y*	—	24,000	51	175	3.0	150
	200–1	120	24,000	24,000	24,000	51	175	3.0	150
34.5	300/173–1	66.4/115	19,900/34,500 *Y*	—	34,500	70	230	3.0	200
	300–1	115	34,500	34,500	34,500	70	230	3.0	200
46	400/231–1	66.4/115	26,600/46,000 *Y*	—	46,000	93	290	3.0	250
	400–1	115	46,000	46,000	46,000	93	290	3.0	250
69	600/346–1	66.4/115	39,800/69,000 *Y*	—	69,000	140	400	3.0	350
	600–1	115	69,000	69,000	69,000	140	400	3.0	350
92	800/462–1	66.4/115	53,100/92,000 *Y*	—	92,000	185	520	3.0	450
	800–1	115	92,000	92,000	92,000	185	520	3.0	450
115	1,000/577–1	66.4/115	66,400/115,000 *Y*	—	115,000	230	630	3.0	550
	1,000–1	115	115,000	115,000	115,000	230	630	3.0	550
138	1,200/693–1	66.4/115	79,700/138,000 *Y*	—	138,000	275	750	3.0	650
	1,200–1	115	138,000	138,000	138,000	275	750	3.0	650
161	1,400/809–1	66.4/115	93,000/161,000 *Y*	—	161,000	325	865	3.0	750
	1,400–1	115	161,000	161,000	161,000	325	865	3.0	750
196	1,700/982–1	66.4/115	113,100/196,000 *Y*	—	196,000	395	1,035	3.0	900
	1,700–1	115	196,000	196,000	196,000	395	1,035	3.0	900
230	2,000/1,155–1	66.4/115	132,800/230,000 *Y*	—	230,000	460	1,210	3.0	1,050
	2,000–1	115	230,000	230,000	230,000	460	1,210	3.0	1,050
287.5	2,500/1,444–1	66.4/115	166,000/257,500 *Y*	—	287,500	575	1,500	3.0	1,300
	2,500–1	115	287,500	287,500	287,500	575	1,500	3.0	1,300
345	3,000/1,733–1	66.4/115	199,200/345,000 *Y*	—	345,000	690	1,785	3.0	1,550
	3,000–1	115	345,000	345,000	335,000	690	1,785	3.0	1,550

[a] Intermediate voltage ratings are placed in the next higher insulation class unless otherwise specified.

[b] Two types of potential transformers are available in insulation classes 25 kv and above. One type is for primary line-to-neutral connection and is provided with a tapped secondary winding, or with two secondary windings, to make available nominal 115 or 120 secondary volts when connected Y or Δ. These transformers are designated in Col. 2 by the double ratio, for example 200/115:1. The other type is for primary line-to-line connection to make available 115 or 120 secondary volts from each transformer. These transformers are designated in Col. 2 by the single ratio, for example 200:1.

[c] When transformer apparatus is used on a Δ-connected system which operates with one phase grounded, special consideration should be given to the selection of the insulation class.

[d] Y-connected transformers for operation with neutral solidly grounded, or grounded through an impedance, may have reduced insulation at the neutral as specified in 2.031.

[e] All potential transformers for Y connection on a three-phase three-wire ungrounded or impedance-grounded system (or on a four-wire system where the ground may be disconnected during a line-to-ground fault) should be designed to have magnetic induction low enough to enable them to operate continuously at line-to-line voltage.

The secondary windings of instrument transformers shall withstand a low-frequency test of 2,500 volts to ground and normally grounded core and other metal parts but no impulse tests are required. See notes (*g*) and (*i*) of Table II.

formers of the same ratio having the higher test which may be connected in wye at their rated excitation voltage.

In the proposed initial draft of American Standard *C*-57, the ratios of potential transformers intended for line-to-neutral connection did not give 115 volts line-to-neutral in all cases when connected wye at the rated voltages indicated. This was felt to be an undesirable situation, and these ratios were adjusted slightly to correct this condition. A double-ratio rating was also given to provide for 115-volt line-to-line as well as line-to-neutral connection.

The new proposal in Table IX is given here under the same table number. The third column, "Secondary Volts," has been added to make clear which transformers have two secondary voltages available. In the fourth column the item, "Name-Plate Rating," has been added to the heading, together with the proper entries in the column, to eliminate the previous confusion which existed as to which transformers were suitable only for line-to-neutral connection. The fifth and sixth columns were added to indicate the circuit voltage for which the transformers were intended. The seventh column was added to point out the limit voltages between line terminal and ground for steady-state operation.

Standard Insulation Classes and Tests for Current Transformers (4.051)

In the initial proposed draft of *C*-57, the insulation tests for current transformers were included in the same table as tests for potential transformers. This obviously led to much confusion and misunderstanding which has been overcome by the use of a separate table for the insulation tests for current transformers. The proposal is given as Table X.

Bushing-Type Current Transformers

The printed draft of *C*-57 did not include any material on bushing-type current transformers. It was felt that this should be done, and wherever practicable, bushing-type current transformers have been covered, but not as completely as could be desired.

Name Plates (4.075)

A new item, "Data Sheet Number," has been added to the required information on name plates. This was done, because in many cases the name plate will not be large enough to include all the information which might be thought desirable. It is the intent that the manufacturer shall have such material available under this number and will be able to furnish it upon request by reference to the proper design number.

It is expected that the "Data Sheet Number" will serve the same general purpose as the instruction book or sheet referred to in 3.090 (16). The "Data Sheet" will be the means whereby the user may obtain such information as thermal and mechanical limits, accuracy classifications, application data, or any other information of general use in the application of instrument transformers.

Standard Insulation Classes and Dielectric Tests for Current Transformers

Table X of Proposed Standard C-57

		Dielectric Tests			
		Low Frequency	Impulse		
			Chopped Wave		Full Wave
Insulation Class (Name-Plate Rating) Col. 1[a]	Maximum Line-to-Line Voltage (Kv) Col. 2	Kv RMS Col. 3[b]	Kv Crest Col. 4[b]	Minimum Time to Flashover μsec. Col. 5[b]	Kv Crest Col. 6[b]
1.2	1.2	10	36	1.0	30
2.5	2.5	15	54	1.25	45
5.0	5.0	19	69	1.5	60
8.66	8.66	26	88	1.6	75
15*L*	15.0	34	110	1.8	95
15*H*	15	34	130	2.0	110
25	25	51	175	3.0	150
34.5	34.5	70	230	3.0	200
46	46	93	290	3.0	250
69	69	140	400	3.0	350
92	92	185	520	3.0	450
115	115	230	630	3.0	550
138	138	275	750	3.0	650
161	161	325	865	3.0	750
196	196	395	1,035	3.0	900
230	230	460	1,210	3.0	1,050
287	287	575	1,500	3.0	1,300
345	345	690	1,785	3.0	1,550

[a] Intermediate voltage ratings are to be placed in the next higher insulation class unless specified otherwise.

The secondary windings of instrument transformer shall withstand a test of 2,500 volts to ground and normally grounded core and other metal parts. No standard impulse tests have been established for secondary windings of instrument transformer.

Appendix I. Development of Expression of Limiting Phase Angle β

In Figure 2 are shown the actual and indicated currents in a current transformer on a 1-1 turn basis. The relationship among the transformer correction factor, TCF, the ratio correction factor, RCF, and the phase angle, β, is developed as follows on the assumption that there is no error in the potential circuit:

$$\text{Actual power} = EI \cos \theta$$

$$\text{Indicated power} = EI_s \cos (\theta - \beta)$$

$$\frac{\text{Actual power}}{\text{Indicated power}} = \frac{I_p}{I_s} \frac{\cos \theta}{\cos (\theta - \beta)}$$

But

$$\frac{\text{Actual power}}{\text{Indicated power}} = \text{TCF by definition}$$

$$\frac{I_p}{I_s} = \text{RCF by definition}$$

so that

$$\text{TCF} = \text{RCF} \frac{\cos \theta}{\cos (\theta - \beta)}$$

$$= \text{RCF} \frac{\cos \theta}{\cos \theta \cos \beta + \sin \theta \sin \beta}$$

A positive angle β indicates in this development that the secondary current is

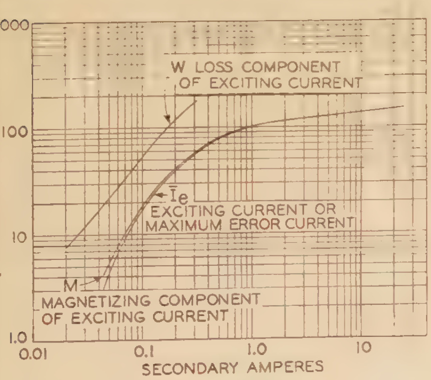


Figure 4. Typical saturation curve and loss and magnetizing component curves of a bushing-type current transformer

60 cycles
Nominal ratio 500/1
Secondary winding resistance 0.19 ohm

Table A

Primary Current Rating	Preferred Ratio
(a) Single-ratio current transformers	
10	2–1
15	3–1
20	4–1
25	5–1
30	6–1
40	8–1
50	10–1
75	15–1
100	20–1
150	30–1
200	40–1
300	60–1
400	80–1
600	120–1
800	160–1
1,200	240–1
1,500	300–1
2,000	400–1
3,000	600–1
4,000	800–1
5,000	1,000–1
(b) Double-ratio current transformers	
10/20	2/4–1
25/50	5/10–1
50/100	10/20–1
75/150	15/30–1
100/200	20/40–1
150/300	30/60–1
200/400	40/80–1
300/600	60/120–1
400/800	80/160–1
600/1,200	120/240–1
(c) Multiratio transformers (bushing type)	
600*	120/100/90/80/60–1 50/40/30/20/10–1
1,200*	240/200/180/160/120-1 100/80/60/40/20–1
2,000*	400/360/320/280–1 240/160–1
3,000*	600/400/320–1
4,000*	800/600/400–1
5,000*	1,000/800/600–1

* Maximum.

ahead of the primary current, from which the following close approximation is obtained:

$$\text{RCF}=\text{TCF}(1+\beta \tan \theta) \quad (\beta \text{ in radians})$$

and then

$$\beta=(\text{RCF}-\text{TCF}) \cot \theta \quad (\beta \text{ in radians})$$

The value of the phase angle β in minutes is given by

$$\beta=3{,}438 \cot \theta \ (\text{RCF}-\text{TCF})$$

For a given power-factor angle θ, phase angle β and ratio correction factor RCF, the value of the transformer correction factor TCF at this condition will not be exceeded for angles less than this value of θ.

Since the maximum angle θ in the transformer accuracy limits is taken as $\cos^{-1}0.6$, then these TCF limits will not be exceeded for any value of θ from $\cos^{-1}0.6$ to $\cos^{-1}1.0$.

With $\cos^{-1} 0.6$

$$\beta=2{,}600 \ (\text{RCF}-\text{TCF}) \text{ very nearly}$$

in which both the maximum and minimum values of β, RCF, and TCF are taken for the purpose of specifying their limits.

Table B

Designation of Burden*	Resistance (Ohms)	Inductance (Millihenrys)	Impedance (Ohms)	Volt-Amperes*	Power Factor
	Burden Characteristics		Secondary Burden at 60 Cycles and Five Amperes Secondary Current		
B-0.1 (X)	0.09	0.116	0.1	2.5	0.9
B-0.2	0.18	0.232	0.2	5.0	0.9
B-0.5 (Y)	0.45	0.580	0.5	12.5	0.9
B-1	0.5	2.3	1.0	25	0.5
B-2 (Z)	1.0	4.6	2.0	50	0.5
B-4	2.0	9.2	4.0	100	0.5
B-8	4.0	18.4	8.0	200	0.5

* In accordance with 1.130 the burden may also be designated by means of the volt-ampere characteristic: that is, va 2.5, or va 50.

Table C

Accuracy Class	Limits of Transformer Correction Factor: 100 Per Cent Rated Current, Minimum	Maximum	10 Per Cent Rated Current, Minimum	Maximum	Limits of Power Factor (Lagging) of Metered Power Load
2.4	0.976	1.024	0.952	1.048	0.6–1.0
1.2	0.988	1.012	0.976	1.024	0.6–1.0
6/10	0.994	1.006	0.988	1.012	0.6–1.0
3/10	0.997	1.003	0.994	1.006	0.6–1.0
5/10	0.995**	1.005**	0.995	1.005	0.6–1.0

** These values also apply to 150 per cent of rated current.

Appendix 2. Calculation of Current-Transformer Ratio-Correction Factor and Phase Angle

Method 1

Starting with a given value of secondary current I_s, determine the internal voltage E_i necessary to force the secondary current I_s through the secondary resistance R_s in series with the external burden Z_b, and the angle ϕ_s by which I_s lags E_i. Express the secondary current vectorially with respect to E_i as a reference.

$$I_s=A-jB$$

From the curve such as Figure 4, determine the exciting current as a vector with respect to same reference voltage E_i.

$$I_e=W-jM$$

Determine the primary current, on the same turn base, by adding vectorially the secondary and exciting currents.

$$I_p=\sqrt{(A+W)^2+(B+M)^2}$$

$$\text{RCF}=I_p/I_s$$

Phase angle β is the angle by which I^s leads I_p

$$\beta=\left(\tan^{-1}\frac{B+M}{A+W}\right)-\phi_s, \text{ in degrees}$$

Table D

Accuracy Class	Limits of Transformer Correction Factor	Limits of Power Factor (Lagging) of Metered Power Load
1.2	1.012–0.988	0.6–1.0
6/10	1.006–0.994	0.6–1.0
3/10	1.003–0.997	0.6–1.0

Table E

Maximum Per Cent Ratio Error	Accuracy Class	Maximum Per Cent Ratio Error	Accuracy Class	Maximum Secondary Terminal Volts*	Maximum Secondary Burden Ohms**
10	10H50	2.5	2.5H50	50	2
10	10H100	2.5	2.5H100	100	4
10	10H200	2.5	2.5H200	200	8
10	10H400	2.5	2.5H400	400	16
10	10H800	2.5	2.5H800	800	32

* Secondary voltage and current limitation—in any accuracy class the specified per cent ratio error shall not be exceeded for the specified secondary terminal voltage at current values from 5 to 20 times rated secondary current.

** Secondary burden and current limitation—in any accuracy class the specified per cent ratio error shall not be exceeded for the specified secondary burden ohms at current values from one to five times rated secondary current.

Method 2

Express the secondary current as

$$I_s=I_s\epsilon^{j\phi_s}$$

and the excitation current as

$$I_e=I_e\epsilon^{j\phi_e}$$

From these the RCF is

$$\text{RCF}=\sqrt{1+2\frac{I_e}{I_s}\cos(\phi_e-\phi_s)+\left(\frac{I_e}{I_s}\right)^2}$$

And β, the angle by which I_s leads I_p is, providing ϕ_s and ϕ_e are looked upon as being positive angles

$$\tan \beta=\frac{I_e \sin(\phi_e-\phi_s)}{I_s+I_e \cos(\phi_e-\phi_s)}$$

RCF and β may be determined directly from the chart given in Figure 5.

Method 3

Let I_s, I_e, ϕ_s, and ϕ_e be the same as in method 2 and let

$$a=\frac{I_e}{I_s} \text{ and } p=a\cos(\phi_s-\phi_e)+\frac{a^2}{2}$$

Then the ratio correction factor

$$\text{RCF}=1+p-\frac{p^2}{2}+\frac{p^3}{2}-+\text{etc.}$$

And the phase angle β, by which I_s leads I_p is, providing ϕ_s and ϕ_e are looked upon as positive angles

$$\tan \beta=\frac{a \sin(\phi_e-\phi_s)}{1+a\cos(\phi_e-\phi_s)}$$

Appendix 3. Excerpts From Proposed American Standard for Transformers C-57 (Currently Proposed Revision of Section 4)

4.021 Preferred Primary Current Ratings and Ratios of Current Transformers

The preferred primary current ratings and ratios of current transformers shall be as shown in Table A.

Standard Burdens and Standard Accuracy Classes for Current Transformers

4.030 Standard Burdens for Rating Purposes

(*a*). Standard burdens for rating purposes shall have constant-impedance ohms and linear-reactance characteristics over the entire current range on which they are used and shall have resistance and inductance values, together with impedance and volt-ampere values, at 60 cycles as shown in Table B.

(*b*). Burden *B*-0.1, *B*-0.5, and *B*-2 are standard burdens for current transformers used for metering service and correspond to the *X*, *Y*, and *Z* designation previously used. Burdens *B*-0.2 and *B*-0.5 are new standard burdens for metering service.

Burdens *B*-5, *B*-1, *B*-2, *B*-4, and *B*-8 are standard burdens for current transformer used for relaying service.

4.031 Standard Accuracy Classes of Current Transformers for Metering Service

(*a*). The accuracy classification of current tranformers for metering service shall be based on the requirement that the transformer correction factor (TCF) shall be within specified limits over a specified range of power factor of the load being metered.

For the purpose of these standards the term "Transformer Correction Factor" (TCF) is used instead of the more complete term "Instrument Transformer Correction Factor" defined in 1.134 to designate the factor by which the watt-hour meter registration or the wattmeter indication must be multiplied to correct for the error introduced by the transformer through the combined effect of the ratio correction factor (RCF) and phase angle *B*.

(*b*). For purposes of standardization the limits of transformer correction factor shall be specified at 100 per cent of rated primary current and at 10 per cent of rated primary current.

(*c*). For the purposes of accuracy classification, the rated secondary current shall be five amperes.

(*d*). The standard accuracy classes and corresponding limits of transformer correction factor (TCF) shall be as shown in Table C.

(*e*). For any known ratio correction factor (RCF) of a given transformer, the positive and negative limiting values of the phase angle β in minutes may be expressed as follows:

$$\beta = 2{,}600(\text{RCF} - \text{TCF})$$

in which TCF is taken as the minimum and maximum transformer correction factor specified in (*d*) above.

The limiting values of the ratio correction factor are the same as the limits of transformer correction factor given in (*d*) above since the phase angle of the current transformer does not introduce a significant error when it is small and when the load power factor is 1.0. These limits of ratio correction factor, together with the corresponding limits of phase angle, keep the transformer correction factor within the specified limits for all values of power factor (lagging) of the measured power load between 0.60 and 1.00.

(*f*). A current transformer shall be given an accuracy rating in accordance with the accuracy class (or classes) in which it falls for the specified standard burden (or burdens). Double-ratio transformers shall be given accuracy ratings for each ratio.

(*g*). In practice, current transformers shall be designated in reference to accuracy rating by the accuracy class number followed by the burden number. For example 6/10 *B*-2 and/or 5/10 *B*-0.1 and so forth.

4.037 Standard Accuracy Classes for Potential Transformers for Metering Service

(*a*). The accuracy classification of potential transformers for metering service shall be based on the requirement that the transformer correction factor (TCF) shall be within specified limits over a specified range of power factor of the load being metered [see paragraph 4.031 (*d*)].

(*b*). Standard output and accuracy ratings of potential transformers shall be on the basis of their standard rated secondary voltage.

(*c*). The standard accuracy classes and corresponding limits of transformer correction factor for potential transformers shall be as shown in Table D.

(*d*). For any known ratio correction factor (RCF) of a given transformer, the positive and negative limiting values of the phase angle "β" in minutes may be expressed as follows:

$$\gamma = 2{,}600(\text{TCF} - \text{RCF})$$

in which TCF is taken as the maximum and minimum transformer correction factor specified in (*c*).

The limiting values of the ratio correction factor are the same as the limits of the transformer correction factor, since the phase angle of the potential transformer does not introduce a significant error when it is small and when the load power factor is 1.0. These limits of ratio correction factor, to-

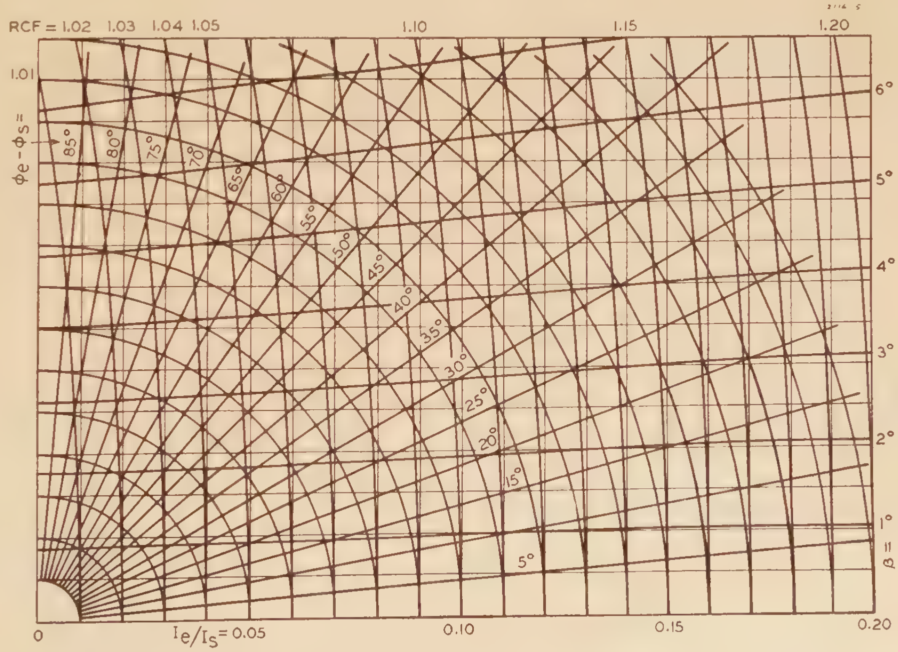
Table F

Maximum Per Cent Ratio Error	Accuracy Class	Maximum Exciting Current	Maximum Per Cent Ratio Error	Accuracy Class	Maximum Exciting Current	Maximum Secondary Terminal Volts*	Maximum Secondary Burden**
10	10*L*50	10	2.5	2.5*L*50	2.5	50	*B*-0.5
10	10*L*100	10	2.5	2.5*L*100	2.5	100	*B*-1
10	10*L*200	10	2.5	2.5*L*200	2.5	200	*B*-2
10	10*L*400	10	2.5	2.5*L*400	2.5	400	*B*-4
10	10*L*800	10	2.5	2.5*L*800	2.5	800	*B*-8

* Secondary voltage and current limitation—in any accuracy class the specified per cent ratio error shall not be exceeded for the specified secondary terminal voltage at 20 times rated secondary current.

** Secondary burden and current limitation—in any accuracy class the specified per cent ratio error shall not be exceeded for the specified secondary burden ohms at current values from 1 to 20 times rated secondary current.

Figure 5. Chart for calculating RCF and β



gether with the corresponding limits of phase angle, keep the transformer correction factor within the specified limits for all values of power factor (lagging) of the measured power load between 0.6 and 1.0.

(*e*). These limits shall apply from 10 per cent above rated voltage to 10 per cent below rated voltage, at rated frequency, and from no burden on the potential transformer to the specified burden (see paragraph 4.002).

(*f*). In practice, a potential transformer shall be designated as 6/10 *W* if its ratio and phase-angle characteristics conform with the requirements of accuracy class 6/10 when used with any secondary burden not exceeding *W* with the power factor specified for *W* burden.

(*g*). In plotting curves of potential transformer accuracy, power factors shall be chosen to conform with the power factors of the standard burdens.

4.032 Standard Accuracy Classes for Current Transformers for Relaying Service

(*a*). 1. The overcurrent performance of current transformers for relaying service shall be specified on the basis of a standard per cent ratio error at 20 times rated secondary current, the standard relaying burdens, and the secondary terminal voltages established by the specified secondary current (20 times rated) operating into these burdens.

In these standards the term "per cent ratio error" is defined as 100(RCF–1), RCF=ratio correction factor.

2. A current transformer shall be given an accuracy rating in accordance with the maximum secondary terminal voltage at which the specified error will not be exceeded on the basis of 20 times normal secondary current operating into a standard relaying burden.

3. Accuracy classification for multiratio current transformers shall be given for the maximum secondary winding, and for double-ratio transformers shall be given for each ratio.

(*b*). Unless otherwise specified the rated secondary current shall be five amperes.

(*c*). For the purposes of standardization the standard specified per cent ratio errors shall be 10 per cent and $2^1/_2$ per cent.

(*d*). The accuracy classes of those transformers having a high internal secondary impedance (wound-type) shall be as shown in Table E.

(*e*). The accuracy classes of those transformers having a low internal secondary impedance (bushing-type) shall be as shown in Table F.

(*f*). The induced secondary voltage and corresponding value of exciting current form a good criterion of transformer performance and burden capacity. The induced secondary voltage is the vector sum of the secondary terminal voltage and the voltage drop due to the internal impedance of the secondary winding. The internal impedance may be negligible in certain designs, such as the bushing type, and quite large in others, such as the wound type.

Transformers having a relatively high internal impedance can maintain constant secondary terminal voltage under load for a range of 5 to 20 times rated secondary current, due to the fact that the internal voltage drop decreases more rapidly with reduction in secondary load current than the induced secondary voltage decreases with lower values of exciting current.

Transformers having relatively low internal impedance do not as a rule have this compensating internal voltage drop and can be standardized at one point only.

Recognition of this fact has resulted in the establishment of separate accuracy specifications for the two forms of current transformers.

(*g*). In practice, current transformers shall be designated by the accuracy class number.

The first number indicates the per cent ratio error. The letter *H* or *L* indicates which type of accuracy is specified. The second number gives the maximum secondary voltage at which the specified accuracy is obtained, with a secondary current of 20 times rated.

For example, a transformer of the 10*H*400 accuracies class has high internal impedance and the ratio error is not more than ten per cent at a secondary terminal voltage not exceeding 400 volts for secondary current values from 25 amperes to 100 amperes, or from 5 times to 20 times rated current.

4.046 Mechanical Limit for Current Transformers

(*a*). The mechanical limit of a current transformer is defined by specifying the maximum rms symmetrical primary current which the transformer is capable of withstanding with its secondary winding short-circuited for six cycles (0.1 second) and which may be in successive applications with no intentional time delay between applications. Transformers shall not be required to withstand a test for the mechanical limit exceeding one-half the thermal limit at six cycles (0.1 second) as calculated from the thermal limit at one second [see section 4.045 (*d*)].

(*b*). The transformer shall be capable of withstanding the mechanical forces produced by this value of current when the current wave is initially completely offset.

4.047 Open Secondary Circuit of Current Transformers

Current transformers conforming to these standards shall be capable of carrying rated primary current continuously with the secondary circuit open, without damage to the transformers, except when the open-circuit voltage exceeds 3,500 volts crest, which usually will occur when the ratio exceeds 300–1. When the secondary peak voltage exceeds 3,500 volts, the secondary of such a transformer shall be insulated so that it can operate continuously with current in the primary to produce 3,500 volts peak across the secondary terminals. (However, the operation of current transformers under such conditions should be guarded against to prevent excessive voltage in the secondary winding.)

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Motor Insulation, Heat, and Moisture

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Insulation Tests

THE subject of nondestructive tests on insulation has received much attention. Since a function of insulation in a given application is to provide a certain minimum dielectric strength, tests at their greatest usefulness would give a measure of dielectric strength without requiring a dielectric breakdown. Additional indications like proneness to loss of dielectric strength if subjected to certain hazards of service are valuable, and information on the deterioration or aging of insulation with service may follow from periodic tests.

Current Practices

Although an ideal test has yet to be devised, the insulation resistance and power-factor methods are widely used for nondestructive tests on insulation. Insulation-resistance measurements are recognized in many test codes and standards.[1,2] A formula is given for the insulation resistance to be expected in a clean dry machine at 75 degrees centigrade. In some cases the same formula is given for a minimum safe value. In 1934 Wieseman[3] pointed out inadequacies of this formula and suggested improvements, and his work seems to deserve more recognition. Dielectric absorption has appreciable influence upon indicated values of insulation resistance but is not mentioned in recent standards like the Test Code for Single-Phase Motors, November 1941. This absorption effect is appreciable and may be taken into account by establishing a definite time of voltage application for making observations. Actually, many of the deductions made by an experienced Megger operator are hard to formulate or standardize.

Capacitance and power-factor measurements have proved valuable for field testing of high-voltage insulation as in bushings or transformers. These measurements are largely detectors of moisture in sealed or oil-insulated types of apparatus.[4,5] For rotating apparatus perfect sealing is difficult, and a varnish film protects the insulation. Appreciable moisture can be tolerated, at least to the extent that power-factor measurements seem less significant.

An extensive and prolonged study of nondestructive tests leading to many improvements in technique has been conducted on very large machines by an Edison Electric Institute committee.[6] Field tests provide convincing evidence of any conclusions or limits established. Laboratory investigations however permit accelerated tests with one variable under study at a time, and dielectric strength tests leading to correlation with the nondestructive indicators are possible. In the belief that a study of testing methods under extreme conditions might prove helpful in interpreting field data and permit suggesting limits based on dielectric strength, some motors of the five-horsepower 440-volt class or smaller have been subjected to severe temperature or moisture conditions.

Laboratory Tests

Two similar five-horsepower three-phase 220–440-volt induction motors have been operated more than two years on an overload cycle. Each working day one motor has been brought to a temperature of 125 degrees centigrade by winding resistance, the other to 100 degrees centigrade. Insulation resistances have been observed before starting and immediately after shutdown each day. The motors have been started across the 230-volt line with 15-kw d-c generators connected. Operation thus has been similar to actual service conditions with one unit delivering about 7.2 horsepower and the other 6.4 horsepower.

Several fractional-horsepower motors have been subjected to high humidity conditions. After several trials with various cooling cycles by refrigeration, the simple arrangement of exposing the samples at room temperature in a closed metal chamber with water in the bottom (appendix I) was adopted. Insulation resistances, dissipation factors* and capacitances on samples so conditioned have been recorded periodically. Dielectric breakdown tests have been made on a number of the samples.

Motors are not suitable samples on which to secure extensive data on dielectric breakdown voltages. Accordingly, small wax-dipped paper-insulated capacitors, which could be conditioned readily, have been used to observe the relation between dielectric strength, insulation resistance, and dissipation factor under exposure to high humidity.

Insulation-Resistance Measurements

Careful technique and analysis show promise of increasing the significance of insulation-resistance measurements. The following considerations should be given attention:

(*a*). The influence of external leads should be eliminated.

(*b*). The effect of time of voltage application should be taken into account by making observations at a definite time such as one minute after test voltage is applied, or by exploring the dielectric-absorption curve.

(*c*). A definite trend in insulation resistance for weeks or months may be of significance where individual values are not.

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* Dissipation factor is the cotangent of the angle of which power factor is the cosine. It is often multiplied by 100 and given in per cent.

(*d*). Measurements at room temperature are attractive for practical reasons.

In both the high-temperature and the moisture tests on motors, leads influenced resistance readings. About ten feet of rubber-insulated cable lying on a wooden floor was connected to each of the five-horsepower motors. The cables were isolated and checked for insulation resistance at the start of the test which happened to be in the winter time. Later it was found, under summer humidity conditions, that the cable resistances were less than those of the motors. Similarly, on the small motors subjected to high humidity, leakage between the leads and frame in the terminal boxes determined observed insulation-resistance values at times. Hence, an essential precaution to obtaining significant values on motor

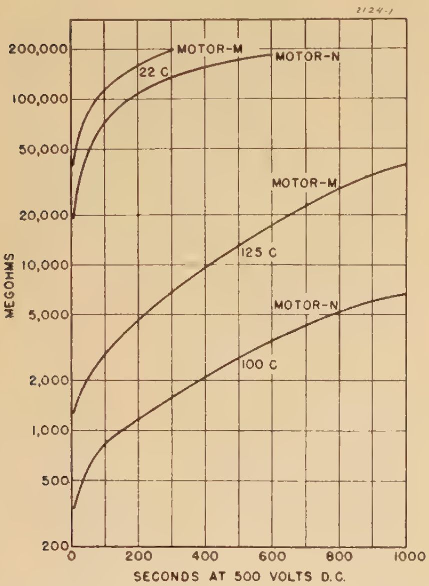

Figure 1. Insulation resistances hot and at room temperature on two five-horsepower motors on daily load cycle

insulation is to make sure that leads[7] are not influencing the results.

The effect of time of voltage application is shown in Figure 1 which gives insulation resistance values before and after one of the daily runs for each of the five-horsepower motors. For the curves while hot, the machines were cooling rapidly from the high temperatures of operation. Thus, cooling as well as dielectric absorption contributed to the increase of apparent insulation resistance with time. The 125 degrees centigrade motor, for instance, in 15 minutes has reached a resistance higher than the five-second value at room temperature. Times longer than 15 minutes appear necessary to arrive at steady-state conditions. These are not practical for small apparatus, but the rapid changes with time indicate the desirability of selecting a definite time arbitrarily for comparative values. For data in this paper insulation resistance values are for one minute after application of 500 volts d-c. Bridges with electronic balance indicators were used.

An enclosed d-c motor operated daily at 165 degrees centigrade has shown a reversal of the trend of change in insulation resistance. With the test started in August, the hot insulation resistance of the armature increased gradually from 2.2 to 1,000 megohms with drying and curing of the insulation. However, after several months operation enclosed and overloaded, the effect of brush dust reversed this trend. It was even more apparent in room-temperature-resistance values. In one case, these dropped from 7,500 megohms to 50 megohms over a period of six months. Simply cleaning the armature with compressed air raised the resistance to 30,000 megohms.

Electrical standards usually suggest a temperature of 75 degrees centigrade for insulation-resistance measurements. This temperature tends to minimize the effects of moisture, but a machine may require many hours or days at 75 degrees centigrade to reach a steady state of insulation resistance. On large generators the pertinent question often is whether continued operation is safe. On small machines the information may be required to determine the advisability of applying voltage and starting operation without drying. Therefore, the usefulness of tests would be enhanced by making measurements and establishing limits for a temperature in the ambient range.

Aging of Insulation

The temperature aging of insulation is difficult to detect from insulation-resistance measurements. It does not seem possible to draw conclusions on the relative insulation condition of the motors operated at 125 degrees centigrade and 100 degrees centigrade, although further experience may disclose significant relations. A physical examination shows that the 125 degrees centigrade insulation is somewhat brittle. Probably, it would be much more vulnerable to long or severe exposure to vibration or moisture. But where the operating schedule is regular, standard temperature limits appear conservative for many conditions of operation. From these tests the following observations were made, applying of course to thoroughly baked insulation:

(*a*). At room temperature the induction motors clean and dry had insulation resistances of from 100 to 100,000 megohms. In summer the values were below 1,000 megohms. In winter in a heated room 10,000 to over 100,000 megohms were obtained.

(*b*). A heated motor showed insulation resistance upon cooling of 10,000 to 1,000,000 megohms or an increase of 10 to 100 times.

(*c*). The 125 degrees centigrade motor had hot resistances of 1,200 to 2,500 megohms after 18 months operation, while the 100 degrees centigrade motor varied between 500 and 1,200 megohms. These values appear to have increased with aging of the insulation, but this point is submerged by the effects of the leads in the early part of the tests. The daily changes with temperature correspond fairly well with temperature correction curves[8] given by Rylander.

(*d*). One feature in comparing results on the two motors is the relative consistency of the resistance values. The day-to-day variations seem closely related.

(Table I shows values for one week. When one motor changed up or down, the other tended to do likewise. Many exceptions occurred, but there seemed to be a definite consistent relation, which suggests the influence of external conditions affecting both motors alike. Possibly it was simply temperature variations resulting from changes in line voltage or ambient temperature. The evidence is good that certain influences were functioning consistently and, that wide variations in insulation resistance may appear more logical with a better understanding of all the variables involved.)

(*e*). During weekends or occasional longer periods of shutdown, insulation resistance values tended to fall. This suggests that the room temperature reading at any given time is influenced by the integrated effect of moisture absorption since the previous heating period. It follows that a single humidity reading at the time of a test is not likely to be significant.

Moisture

Insulation is affected by exposure to moisture in spite of varnish treatments and usual methods of enclosure. These

Table I. Insulation Resistances in Megohms on Two Five-Horsepower Motors Following Daily Load Runs

Motor	Mon	Tues	Wed	Thurs	Fri
125 C	1,100	750	1,300	2,000	1,500
100 C	700	650	700	800	750

Table A

Operating Voltage	Minimum Megohms 20 C–30 C	AIEE Formula Megohms 75 C
110	0.1	0.11
220	0.5	0.22
440	3	0.44
550	5	0.55

factors influence the rate of moisture absorption and drying. On motors standing idle, the absorption is cumulative and appears to be readily reversible.

Four 1/4-horsepower three-phase 440-volt enclosed motors were used as test samples. Measurements were made under the following conditions:

(*a*). As received from the factory.

(*b*). After five hours at 48 degrees centigrade.

(*c*). After 25 hours at 74.5 degrees centigrade.

(*d*). After 44 hours at 95 degrees centigrade.

(*e*). During 18 days standing at room temperature.

(*f*). After 15 hours at five degrees centigrade and seven hours at room temperature.

These test conditions were consecutive and represent a drying cycle, an idle period, and a cooling cycle conducive to condensation. Figure 2 gives capacitances and dissipation factors observed with a 115-volt 60-cycle bridge for conditions (*a*) to (*e*) inclusive on one motor, and the others were quite consistent. The record of moisture absorption is definite. The 30 per cent dissipation factor at the start and the decrease in capacitance and dissipation factor with drying are characteristic of moisture. The later increase in dissipation factor and capacitance when exposed to prevailing summer humidities indicates a slow but definite reabsorption of moisture. The rate varies appreciably and the trend reverses at times. In 18 days the original condition was not yet attained again.

Table II gives insulation-resistance data on the four motors for conditions (*a*) to (*f*) except that only one set of values was recorded during the 18-day idle period. The consistency in performance of the four motors is noteworthy. In all cases values on the different motors are of the same order of magnitude under given conditions. Columns 2, 6, and 7, however, are all for room-temperature measurements at different stages of moisture absorption. Drying increased insulation resistances about 100 times. A cooling cycle conducive to condensation later caused a 1,000 to 1 decrease. In analyzing measurements made at room temperature these variations must be recognized.

Table II. Insulation Resistance in Megohms for Four Small 440-Volt Motors at Controlled Temperatures

1 Motor	2 28 C	3 48 C 5 Hrs	4 75 C 20 Hrs	5 95 C 19 Hrs	6 22 C 23 Hrs	7 25 C (18 Days) 5 C (15 Hrs) 25 C (7 Hrs)
A	460	125	430	415	22,000	27
B	475	119	400	445	65,000	16
C	455	99	340	360	42,000	25
D	495	106	310	340	38,000	31

Accelerated test data for motor *A* without rotor or end bells in the 100 per cent humidity test chamber are given in Table III. After a few days dissipation factor and capacitance increased to the point where readings could not be obtained. The insulation resistance decreased steadily with time of exposure to well below one megohm, and the process seems to be an extension of the rate and the degree observed at ambient humidity.

A number of used motors which had been in storage for years were tested. The dissipation factors were between 30 and 45 per cent. Motor insulation appears to absorb moisture to this extent under normal conditions of storage in summer, and this effect has been observed on idle generators.[6] New and used motors do not appear greatly different unless in the time element involved following a drying treatment.

Further moisture-exposure tests were made on the four motors after redrying. Figure 3 shows the insulation-resistance data.

Curve *A*—Stator only as in Table III.

Cuve *B*—Retreated stator with a different varnish.

Curve *C*—Same as *A*, but enclosed and with synthetic tubing leads to eliminate lead leakage.

Curve *D*—Retreated stator with a different varnish immediately after drying.

The motors were exposed at high humidity for 71 days, and insulation resistances decreased steadily and consistently. Dissipation factors also indicated similar absorption with time, but readings were less consistent in the later stages of the test.

Curve *A*, Figure 3, duplicates the similar test of Table III rather well. By comparison, *C* shows that enclosure retarded absorption materially. Better varnish treatment in cases *B* and *D* did likewise and this result agrees with statistical service data. Improved results have been obtained under some service conditions with additional and special varnishes. These protective measures influence the insulation state reached in a given time.

For part of the test, a voltage of 115 and later 230 volts, 60-cycle, was applied between the windings and iron of stator *A*, Figure 3. When this voltage was removed, the insulation resistance dropped from a 0.4-megohm level to 0.08 megohm. No improvement occurred on application of the voltage, but further depreciation was prevented. The evidence is meager, but further study of

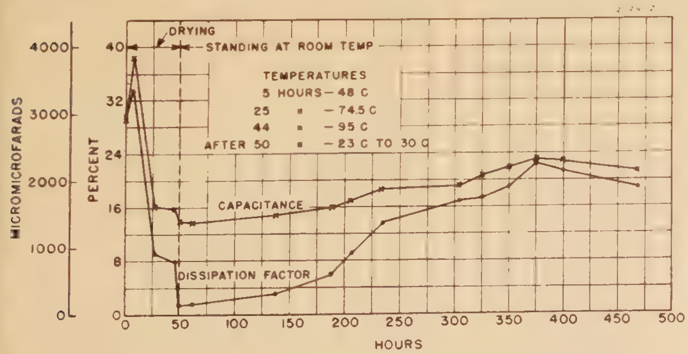

Figure 2. Effect of drying and exposure at summer humidity conditions on capacitance and dissipation factor of small motor insulation

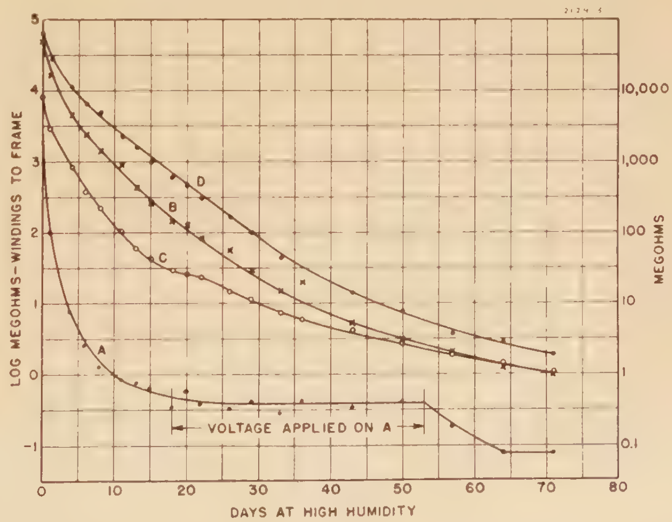

Figure 3 (right). Insulation resistances of small motors in humidity-conditioning chamber

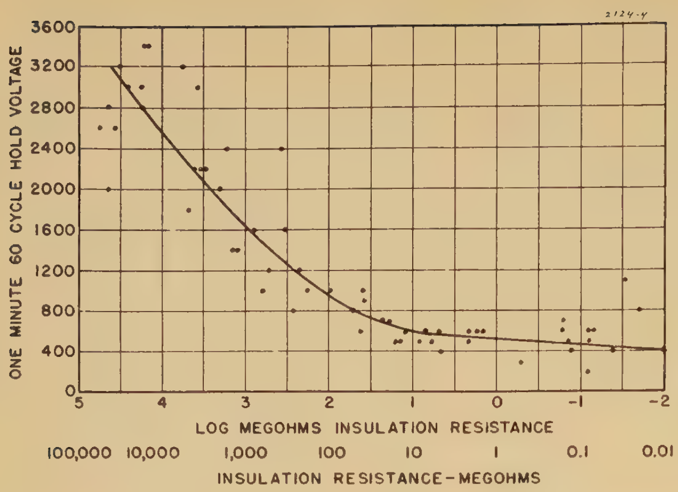

Figure 4. Relation between dielectric strength and insulation resistance on capacitors in moisture-exposure tests

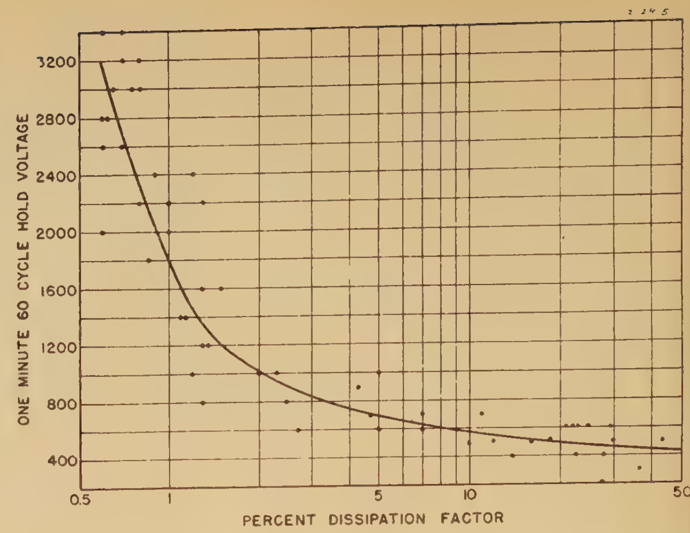

Figure 5. Relation between dielectric strength and dissipation factor on capacitors in moisture-exposure tests

the value of keeping some voltage on insulation to ground under adverse moisture conditions seems warranted. Unfortunately, this measure is not readily applicable to intercoil insulation.

The process of moisture absorption appears to be reversible. If the insulation is dried or even stored in a heated room, recovery is appreciable and rather rapid. The motors were dried between the tests of Table III and Figure 3 apparently without affecting later performance. Table IV gives readings on the stators three days after completion of the tests of Figure 3. Insulation resistances and dissipation factors, in spite of dielectric breakdown tests in the meantime, indicate appreciable drying has occurred at room temperature. The rate depends again upon the varnish treatment. The problem resolves into avoiding the application of voltage when the insulation is in a weakened condition.

Dielectric Strength

The real criterion of suitability for service is the dielectric strength of the insulation. Moisture reduces dielectric strength appreciably.

Following the exposure tests of Figure 3, dielectric breakdown tests were made on the four motors as in Table IV. The dielectric test for new motors of this class is twice normal plus 1,000 volts for one minute. Two of the stators, *A* and *C*, were below this value following the moisture exposure tests. However, the poorer one held 1,000 volts for one minute, which leaves a little margin over operating voltage in spite of severe test conditions. The motors operated at 100 degrees centigrade and 125 degrees centigrade for two years stood a 2,000-volt momentary test after each year of operation.

Maximum 60-cycle test voltages held for one minute between motor windings and frame for different values of insulation resistance are given in Figure 7. The points at or above 4,000 volts are for new stators, and other points are following different times of exposure to moisture. The curve is drawn near the lower limit of the points. Since the points are the highest voltages held for one minute, and failures occurred at the next highest 200-volt step, the curve is of the nature of a minimum breakdown voltage curve, although a larger number of tests probably would modify it some. There is a fairly definite relation between insulation resistance and dielectric strength for these fractional-horsepower motors.

The capacitors used for dielectric breakdown tests have three layers of about 0.0005-inch paper insulation and are rated at 220 volts alternating current. Operating volts per mil are much higher than in motors, and very little moisture can be tolerated. Samples were exposed at high humidity, and a few were removed periodically and tested for insulation resistance, dissipation factor, and dielectric breakdown strength, as shown in Figures 4 and 5. There seems to be reasonably good correlation between the electrical test values and dielectric strength. For insulation resistances below 100 megohms or dissipation factors over two per cent, the dielectric strength is reduced appreciably to rapidly approach a minimum value.

These capacitors also illustrate another phenomenon on the insulation-resistance test. Figure 6 shows the variation of insulation resistance with time for a sample after 24 days exposure. The insulation resistance first increased with time as in the usual dielectric-absorption curve. After about 20 seconds the trend reversed, and after 40 seconds insulation resistance decreased rapidly with time, possibly because of a partial breakdown at 500 volts direct current. Recovery was rapid and fairly complete, however, as the sample immediately afterwards held 600 volts 60-cycle for one minute and failed at 700 volts. The same action has been observed on motors after appreciable exposure. Davis and Leftwich[9] have reported the decreasing resistance section of the curve due to defective auxiliary apparatus on generators. These effects accent the desirability of observing insulation resistance at one minute or more after voltage is applied. A 10- or 15-second Megger reading would not have disclosed the situation. Also, in this

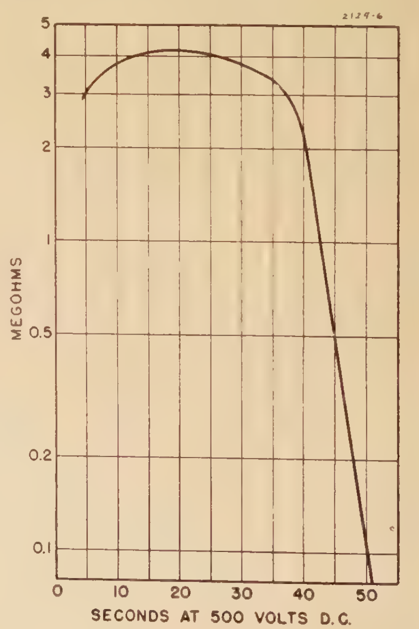

Figure 6. Insulation resistance on capacitor after considerable exposure to moisture

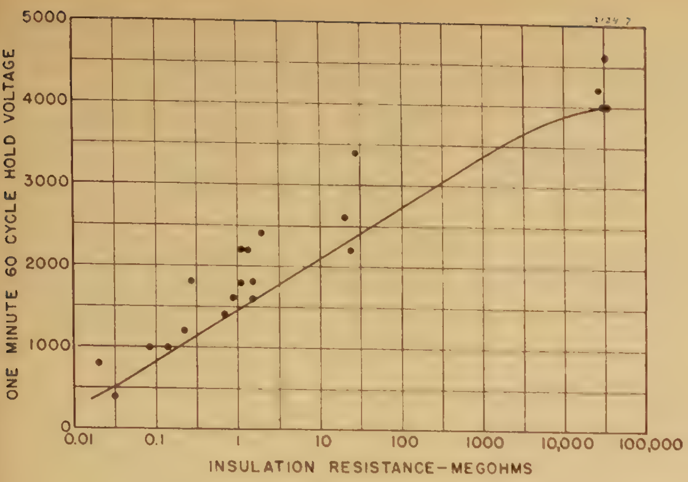

Figure 7. Relation between dielectric strength and insulation resistance for fractional-horsepower 110- to 440-volt motors

case at least, a weakened condition of the insulation was detected at a d-c voltage of one-half the 60-cycle crest breakdown value. These capacitor samples are small, however, and results may have been affected by heating or drying at test voltages.

Nondestructive Tests Versus Dielectric Strength

The purpose of these investigations is, of course, to determine if nondestructive insulation tests can be relied upon to indicate the suitability of apparatus for service.

1. There is some possibility of establishing for some devices minimum insulation-resistance values which will assure satisfactory operation in spite of appreciable moisture absorption.

2. The test methods studied do not appear dependable for revealing insulation defects other than moisture absorption, with the possible exception of dirt.

To establish satisfactory limits for motors considerable dielectric-strength data and field experience should be available. Table IV and Figure 7 indicate, however, that insulation strength decreases with insulation resistance. This

Table III. Measurements Between Windings and Frame on Stator A Exposed to 100 Per Cent Humidity

Days Exposed	Megohms	$\mu\mu$f	Per Cent Dissipation Factor
0	200,000	1,275	1.2
1	5,100	1,420	5.1
5		7,100	38
6	3.1	8,200*	> 50*
7	1.5	11,100	> 50
8	0.85	14,800	> 50
11	0.42	24,200	> 50
12	0.31	30,000	> 50
13	0.21	30,000	> 50

* These and later readings are very approximate as the bridge could not be balanced for dissipation factors greater than 50 per cent.

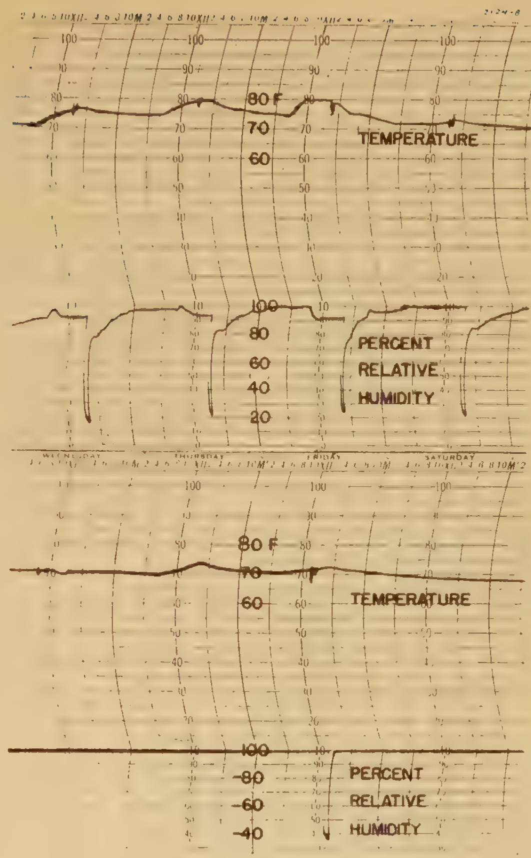

Figure 8 (right). Temperature and humidity chart from metal test chamber with water in the bottom

is recognized in the standards formula for expected values at 75 degrees centigrade. However, the 75 degrees centigrade temperature implies some drying. Also, no time of voltage application is specified, which leaves to chance the inclusion of such effects as in Figure 6. Recognizing the greater usefulness of tests at room temperature, it may be reasonable to suggest from Figure 7 the following tentative limits above which small motors can be safely connected to the line for starting. The values in column 2 are for room temperature at one minute after the d-c test voltage is applied. A ratio of about 60 has been given[8] for values taken at 25 degrees centigrade and 75 degrees centigrade.

These limits allow some margin of safety at operating voltages, but wide variations in practice are to be expected. Appreciably lower values may not prove fatal in many cases. Also, where moisture is not involved, these limits may give no assurance of a satisfactory condition. In fact much higher values often may be associated with poor insulation. Additional experience on many sizes, types, and makes of motors under service conditions will be necessary for proper determination of the best values, but the possibility of establishing useful limits seems good.

The detection of defects under dry conditions is very uncertain for low-voltage apparatus. The roasted condition of the motor operated at 125 degrees centigrade was not apparent from the electrical tests. Columns 5 and 6 of Table IV also are significant. The motors had been tested to failure, but insulation resistances of 1,950, 2.1, and 3.2 megohms were later recorded for three of the windings. Motor *C* did show evidence of the previous breakdown by its zero insulation resistance. It is quite likely that higher insulation test voltages would reveal the condition in all cases. There is always danger, however, in testing at voltages much above operating voltage, or the nondestructive feature of the test may be sacrificed. The failure of insulation-resistance test voltages to disclose punctures or breaks in insulation is readily understandable. The smallest air gap will not sparkover much below 1,000 volts in short-time tests. The separation between conductor and core in small motors may be approximately 0.05 inch, and the dielectric breakdown of air for this distance usually exceeds 4,000 volts with creepage strengths somewhat lower. Consequently, if the puncture or fault is clean and reasonably dry, the insulation resistance may be affected to a small extent. The strength of the insulation between turns, coils, and phases in motors also is important. However, under

Table IV. Tests on Stators After 71 Days at 100 Per Cent Humidity as in Figure 4

	As Removed			After 3 Days in a Heated Room	
1	2	3	4	5	6
Motor	Megohms	Per Cent Dissipation Factor	Volts Held for 1 Minute	Megohms	Per Cent Dissipation Factor
A	0.083	> 50	1,000	1,950	17.5
B	0.88	40	1,600	2.1	31
C	1.08	> 50	2,200	0	15
D	1.95	37	2,400	3.2	34.5

service conditions, the testing of this insulation becomes rather elaborate. In a well-proportioned machine test results on the ground insulation should be indicative of general machine condition.

Conclusions

1. Test codes and standards should recognize the effect of dielectric absorption on insulation resistance and suggest a time such as one minute after voltage application for observing resistance.

2. For industrial apparatus rated below 600 volts room-temperature measurements of insulation resistance seem more practical than 75 degrees centigrade values.

3. Aging of insulation due to temperature is difficult to detect from electrical measurements. These indicators are sensitive to moisture absorption and possibly to dirt.

4. There appears to be a very good correlation between insulation resistance as affected by moisture and dielectric strength for fractional-horsepower motors.

5. Minimum room-temperature values of insulation resistance for starting small motors are suggested.

Appendix I. Humidity-Conditioning Chamber

In seeking a convenient testing cycle at high humidity, several combinations of cooling and exposure to moist atmospheres were tried. It was difficult to reproduce conditions, and considerable handling of the samples was required. The simple expedient of a closed metal can with water in the bottom finally was adopted. For most of these tests the chamber consisted of a can about 30 inches in diameter by 36 inches high with a deep lid about 34 inches in diameter. The assembly was turned upside down from the usual position, and the lid filled with water. Thus, the water provided both a seal and a source of moisture. A pinhole permitted entrapped air to escape. No temperature or humidity controls were provided other than the laboratory heating system which determined ambient conditions. Both temperature and humidity in the chamber were recorded with a Friez Hythergraph, Figure 8. These records indicate a saturated atmosphere most of the time, but this depended to a certain extent upon the condition of the samples and the frequency of opening the chamber. The time to return to maximum humidity after opening the chamber varied with ambient humidity and the duration of the open condition. Times from 0.5 to 20 hours were observed. This might be a handicap in short-time tests, but a fan should speed evaporation appreciably. For insulation tests of this kind, this type of chamber provides a simple, convenient, and easily reproducible conditioning atmosphere. The correlation to actual service conditions with wide temperature and humidity changes and moving air is problematical, but that, relatively, it is a very severe moisture-exposure test seems certain. One possible case which it does simulate closely occurs where water penetrates and remains inside an idle enclosed machine for some time.

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Ignitron Rectifiers in Industry

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WHEN a new device is developed, it is usually adopted and proved by a particular industry. In the case of the ignitron rectifier, the first applications were in transportation service in mines and railways. The apparatus and its performance in these early installations were discussed by the present authors in an earlier Institute paper.[1]

Since that time, as superior operating results became known, the ignitron has been taken up by other industries, most notably the electrochemical. In excess of 2,000,000 kw of ignitron-rectifier units have been purchased by that industry alone. This paper will discuss primarily installations for large power concentrations.

Rectifier Description

Continuously pumped ignitrons have been applied to date in four sizes which, in six tube assemblies, have continuous ratings at 600 volts of 1,250, 1,667, 2,500, and 4,000 amperes. The 2,500-ampere size has been applied in greatest numbers. However, larger-capacity sections are desired for the larger installations, and so the majority of applications have been made using 12-tube assemblies. One design will be described.

The ignitron consists of a drawn steel tank with a removable cover plate into which is mounted the main anode bushing and anode shield terminal. Graphite continues to be the best material for rectifier anodes. The deionizing shield surrounding the anode and the baffle between anode and cathode pool, are also of graphite. This latter is insulated from the cathode to avoid the transfer of the cathode spot from cathode to baffle when high overloads are applied. The tank itself constitutes the cathode connection, since the ignitron does not require an insulated cathode and is bolted to the cathode copper bars on the assembly frame. The ignitor is mounted on a rod which passes through the side of the tank, insulated with a glass-kovar seal, on a flexible steel diaphragm. The mounting is provided with adjusting screws to enable adjustment of the ignitor from outside the tank. Solder-to-porcelain vacuum-tight seals are used for the main anode bushing. A small solid radiator terminal holds this seal within safe temperatures. The low-current potential connection to the anode shield is made through an aviation spark-plug-type bushing with a soft copper gasket. The vacuum-sealing gaskets in the separable joints of the ignitron proper, are enameled aluminum wire rings. Such gaskets operate satisfactorily at 100 degrees centigrade, and the rectifier operating temperature is objectionably close to the maximum safe temperature of rubber. However, rubber constitutes a most convenient, and semiflexible vacuum-tight gasket which requires relatively low flange pressures. Therefore, the external connections to the vacuum pumping system, which operate at low temperatures, are gasketed with iron-band-protected rubber. The ignitron tanks are cooled by water passed through copper tubing, which is wrapped around the tank and coiled on the bottom, and attached to the steel with high-temperature solder. Additional cooling in high-current units is provided by an internal steel coil. This latter is copper-lined to keep all water-cooled surfaces nonferrous. Figure 2 shows a cross-section view and Figure 3 an external view of the ignitron described.

A rectifier unit consists of a transformer and the rectifier assembly or assemblies to which it is connected. In large-capacity units two or more rectifier assemblies may be connected to one transformer.

Rectifier circuits which efficiently utilize the circuit elements are arranged for three-phase operation and require multiples of three anodes. (Smaller installations are sometimes made with four anodes.) Where the power required can be supplied with six anodes, or where the small number of units suggests sectionalization to insure continuity of service, ignitrons are assembled in groups of six on one frame with one cooling system and one vacuum pumping and measuring system. However, in large stations where ample reserve is maintained with one unit out of service, simplicity dictates assembly in units of 12. Such assemblies have proved most convenient, and one vacuum system of any established type has proved adequate. The individual tanks are connected to the vacuum pumps through a vacuum manifold with water-cooled pumping connections which condense the mercury vapor and prevent transfer of mercury between tanks and pump. On these large-size tubes an individual vacuum valve on each facilitates both manufacture and repair, since vacuum tightness can be checked more easily by isolating smaller elements. Also, one ignitron tube can be treated as an entity and interchanged with a spare in less than one hour if necessary.

Figure 4 shows an assembly of 12 ignitrons rated at 5,000 amperes d-c in the 600-volt class.

All high-power rectifiers have been water-cooled. Since cooling water which is pure enough to pass through the rectifier jackets or tubes directly without trouble due to corrosion or scale is rarely available, practically all installations are provided with heat exchangers which permit the recirculation of pure or treated

Figure 1. Typical ignitron installation in an electrochemical plant

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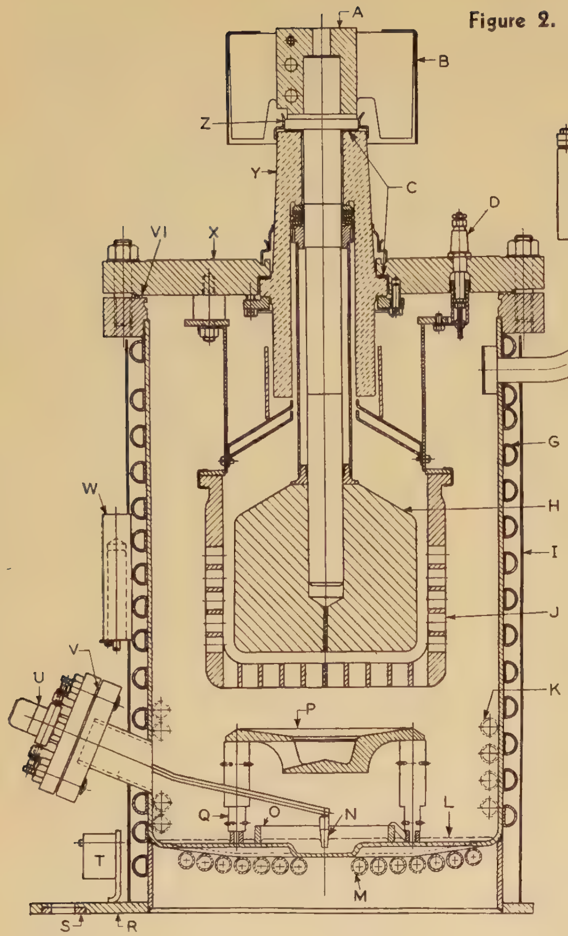


Figure 2. Cross-section view of an ignitron

A—Anode radiator and terminal
B—Radiator cover
C—Asbestos seal
D—Shield entrance bushings
E—Tank valve
F—Vacuum manifold connection
G—Tank-cooling coils
H—Anode head
I—Tank cover
J—Anode shield
K—Internal cooling coils
L—Mercury cathode
M—Cathode-cooling coils
N—Ignitor tip
O—Quartz ring
P—Baffle
Q—Quartz tube
R—Steel plate
S—Copper insert
T—Terminal block
U—Ignitor entrance assembly
V, V1—Aluminum seal rings
W—Thermal switch
X—Anode plate
Y—Anode porcelain
Z—Solder seal

water. Where ample raw water is available, water-to-water exchangers are used, otherwise water-to-air. Water-to-air exchangers are nearly always used in mining installations where cooling water is either not available or of very low quality.

Unit Excitation and Control

The earlier types of ignitron excitation which utilized thyratron tubes have mostly been superseded by circuits which generate impulse voltages by means of saturating reactors.[2] These circuits utilize static devices throughout: that is, simple transformers, reactors, capacitors, and Rectox rectifiers. The excitation-circuit devices, together with the vacuum- and cooling-system control, are mounted in a cabinet located on or near the rectifier. Two types of phase control of rectifier voltage have been used. One is a mechanical phase shifter, which consists of a wound-rotor induction machine. The excitation power is passed through this phase shifter, and the phase position of the excitation impulses is determined by the position of the phase-shifter rotor in its stator. This position is controlled by a fractional-horsepower motor through a gear box. The other type of phase control utilizes some form of phase-shifting network in which the phase position of the excitation impulses is determined by the value of direct current in a saturable reactor. This latter is a less simple circuit than the mechanical phase shifter, but is more flexible where automatic control is required, and speed of response is important. Particularly, the phase-shifting network is suitable for use with a simple type of voltage regulator such as the Silverstat. Also, such a network may be designed to include voltage-compensating characteristics which will maintain full energy excitation through supply-line voltage fluctuations as great as 50 per cent.

Station Layout

The simplest and most economical arrangement of apparatus in a rectifier substation is to effect a straight line run of power from the a-c supply source to the d-c bus. Figures 5 and 6 show a typical multiunit station layout having this arrangement. Incoming power is delivered to the a-c bus, from which it is distributed to the ignitron units through a-c switchgear, the transformers, ignitrons, and d-c switchgear to the d-c bus.

For the layout shown, the a-c switchgear is in a small masonry house separated from the main building. This can be modified to supply conventional outdoor switchgear, outdoor metal-enclosed gear, or the switchgear can be located in the substation building.

All transformer equipment is outdoor. It can be supplied with conventional bushings and an overhead bus structure or with potheads and an underground system as shown.

The substation building is designed with two levels, the ignitrons and all necessary operator control devices being located on the operating floor with some accessories and low-voltage switchgear in the basement. All of the equipment in the basement is operated electrically from the operating floor and is completely supervised by indicating devices. The operator need go to the basement only for routine inspection and maintenance. In many installations the low-voltage switchgear is also located on the operating floor.

Some detail equipment which is required for station operation and which can be located as convenient is not shown. This includes the station battery with charging equipment, the degassing equipment, air-cleaning apparatus if required, storage space for spare parts, and equipment for supplying power to plant auxiliaries which are not associated with the substation equipment.

Building appurtenances such as stairways, lavatory facilities, and operators' office must be included. A repair bay should be provided and is usually at the end of the substation building. Crane facilities and sufficient head room for untanking the largest transformer will determine its construction. An enclosed clean room for working on interior parts of rectifiers is desirable.

Rectifier-Switching Arrangements

Figure 7 shows the several switching arrangements which are in use for ignitron units, rated at 8,000 to 10,000 amperes in the 600 d-c voltage class, in multiunit substations.

Circuit *A* is used when the concentration of power supply to the a-c bus is heavy, a condition which would require large rupturing capacity and costly oil circuit breakers in the rectifier-unit circuits. Circuit breakers are not used, the primary switching consisting of a disconnecting switch which is capable of opening the transformer magnetizing current. For this arrangement, primary faults must be opened by the switchgear in the supply circuit or circuits to the plant. This involves a plant shutdown until the faulty circuit is isolated by operation of the disconnecting switches.

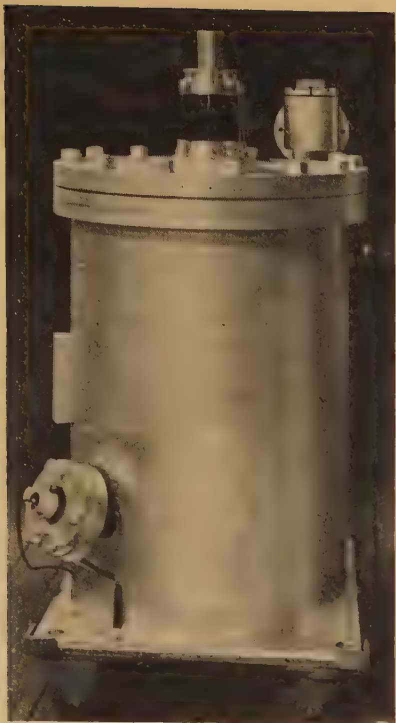


Figure 3. A single ignitron

The secondary switching consists of high-speed anode switchgear with one pole for each phase of the transformer secondary.

High-speed switching, which will limit the d-c rise in the average circuit in the order of one-third cycle, on a 60-cycle basis, is essential for large multiunit substations. At the time of arc back, all normal units will contribute direct current to the arc back in the form of reverse current. This current increases rapidly, depending upon the inductance of the circuit, and must be limited by the switchgear to a value which the switchgear can open successfully and without undue maintenance. It is also advantageous to limit the stresses on the other equipment involved in the circuit, particularly the transformer.

When an arc back occurs, the sound anodes of the ignitron assembly in which the arc back has occurred will supply current to the faulty anode. This feed to the arc back is in addition to the back feed from other ignitron units and is a short circuit, through the arcs involved, on the transformer secondary. This fault must be suppressed by opening the a-c supply to the transformer or by opening the faulty circuit between the transformer and the ignitron.

For arrangement *A*, the a-c supply to the transformer cannot be opened except by operation of the circuit breakers in the station supply. High-speed anode switching is therefore essential for this circuit.

The anode circuit breakers are supplemented by semihigh-speed cathode circuit breakers. These are used for normal switching operations. Disconnecting switches permit maintenance of the circuit breakers with a minimum of equipment out of service.

Circuit *B* is quite similar to circuit *A* except that an oil circuit breaker is used in the circuit to the rectifier transformer. Its use can be justified if the rupturing capacity required is 500,000 kva or less. The circuit breaker is then relatively inexpensive, and each ignitron circuit is completely independent of the others. Circuit faults are isolated without plant shutdown. Complete flexibility of control is afforded.

The use of high-speed anode switching makes it unnecessary to open the oil circuit breaker at the time of arc back. When two ignitron assemblies with independent low-voltage switching are connected to one transformer, the anode switching can be opened for one assembly without affecting operation of the other. This is of advantage in keeping maximum conversion equipment in service. The importance of this feature is in inverse ratio to the number of conversion units normally connected to the common d-c bus.

Circuit *C* is similar to circuit *B* except that the high-speed anode switches are omitted, and a high-speed cathode circuit breaker is substituted for the semihigh-speed switch. With this arrangement reverse current to the ignitron assembly which has arced back is opened with high-speed action by the cathode circuit breaker. Arc quenching may be used to suppress the alternating fault current by preventing the normal anodes of the assembly from firing to the faulty anode. This latter operation is effective in approximately one cycle.

Circuits *B* and *C* are therefore comparable except for two features. Arc quenching sometimes fails in its operation. It is about 90 per cent effective. When it fails in operation, the oil circuit breaker must be opened by overload relay action to remove the fault. Anode switching is fully operative, but there are six poles of switchgear to maintain, compared to a single larger pole of cathode switching. The choice between the two circuits is therefore dictated by consideration of maintenance, as compared with an occasional failure to segregate the fault in a half-rectifier unit.

If arc quenching is not used in connection with circuit *C*, the primary oil switch unit must be interlocked with the cathode switch so that the entire unit is removed from service in the event of arc back, and the a-c short circuit endures for the opening time of the oil switch, which is usually of the order of six cycles.

Circuit *C* makes use of anode disconnecting switches in the anode circuits to each ignitron section, one assembly making up one half of the unit. These are used so that each assembly can be isolated for maintenance and continue with normal operation of its associated assembly. They are also of use during the degassing process.

For those installations, where a complete unit outage is permitted because of spare capacity available, the anode disconnecting switches can be omitted. This applies equally to circuits *A*, *B*, or *C*. Circuit *D* shows a circuit similar to circuit *C* except with the anode disconnecting switches omitted.

For single rectifier units connected to an isolated d-c bus, the switching can be simplified. The unit may consist of one or more ignitron assemblies. For either condition, the amount of current in-

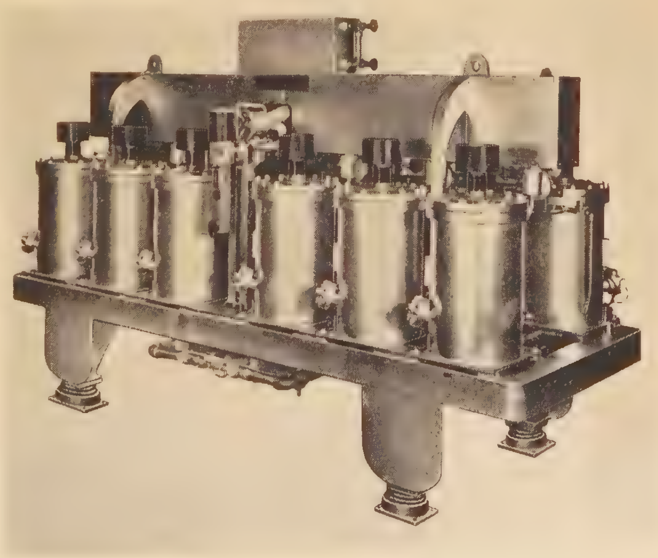
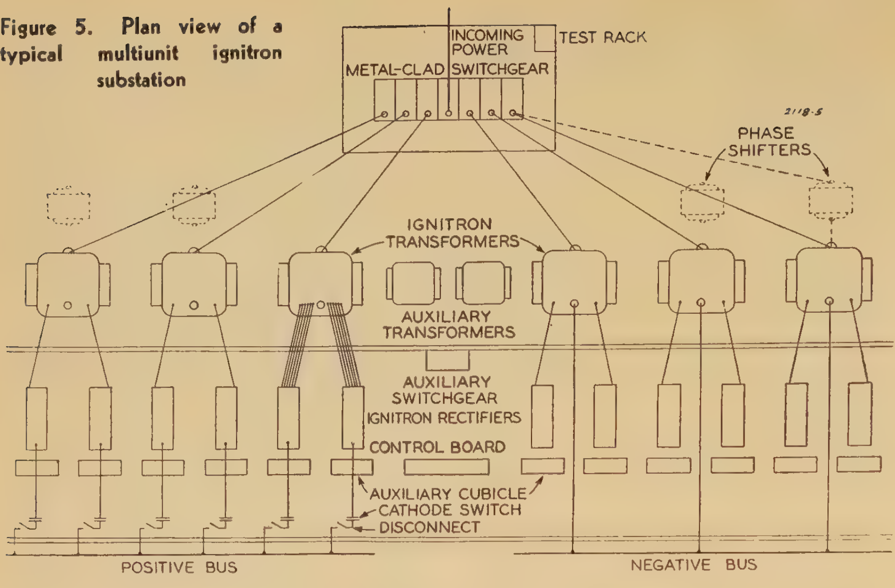


Figure 4. An assembly of 12 ignitrons rated at 5,000-amperes d-c in the 600-volt class

Figure 5. Plan view of a typical multiunit ignitron substation



volved in an arc back is relatively small and semihigh-speed switchgear, having a speed of $1^1/_2$ to 2 cycles to current limitation, is adequate for the service. This may be arranged, as in the case of high-speed switching, either with anode breakers with suitable cathode switching to provide the type of operation desired, or with a cathode breaker and an oil switch in the primary of the transformer. Reverse current for a single assembly is applied from the electrolytic cells. Where a cathode switch is used, an arc back is cleared by the opening of the cathode circuit breaker, followed by the opening of the oil circuit breaker.

For double assembly units, arc quenching can be used in combination with the semihigh-speed cathode switches to secure continuous half-unit operation. Anode disconnecting switches can be used for half-unit isolation. Circuit *E* illustrates the use of semihigh-speed cathode switching with a single rectifier unit supplying an isolated d-c bus.

A special use can be made of high-speed anode switching for a single rectifier unit supplying an isolated load where continuous service is an absolute essential. The switch can be arranged to trip individual poles and on reverse current only. At the time of arc back only the pole of the circuit involved in the fault will open, the other anodes continuing in service.

Station Voltage Control

The great majority of industrial loads requiring one- or two-unit stations, particularly including electric railways, are adapted to the normal five to eight per cent shunt characteristic of noncontrolled rectifiers, and phase control of voltage is not necessary. For applications requiring flatter voltage regulation one of the phase-control systems is used, and there is a wide choice of control method ranging from manual to fully automatic. Provision can be made for flat compounding and cross compounding where more than one unit on a bus is involved. Where small units are located at the end of a low power feeder, which frequently occurs in mines, and the a-c supply to the rectifier transformer varies widely, some form of compensator is used which by phase control reduces the voltage at the lighter loads and provides a nearly flat voltage regulation from light load to full load. Above full load the uncompensated voltage regulation prevails.

In large stations containing many units on one bus, phase control is always used, primarily to compensate for minor differences in impedances and equalize the currents in the several units, and to provide small variations in voltages as desired for the load. However, since large phase-control angles adversely affect power factor and increase harmonics, the minimum feasible transformer tap is used in order to keep the phase control angles at a minimum.

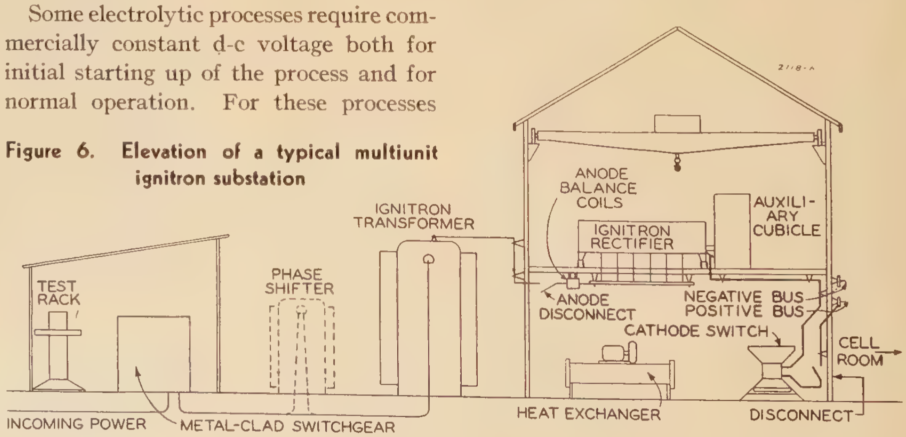
Some electrolytic processes require commercially constant d-c voltage both for initial starting up of the process and for normal operation. For these processes no-load voltage taps can be provided in the rectifier transformers to compensate for seasonal voltage changes and for electrolytic cell aging.

Ignitor or phase control of d-c voltage can be used for short times in the range required to pick up and drop load due to switching requirements. It can also be used for small voltage adjustments over long-time periods of operation.

Some processes require a wide voltage range for initial starting up in order to condition new cells in small blocks, but require essentially constant voltage for normal operation. For these processes an autotransformer of sufficient capacity to supply all rectifiers connected to one or several cell strings in parallel can be supplied. No-load taps are supplied in sufficient number and range to accomplish the desired results. Phase voltage control can be used for d-c voltage adjustment between taps. It is not however recommended for long-time wide-range voltage adjustment, because of its adverse effect on power factor and the increase in harmonics. The autotransformer can also be used for permanent adjustment of d-c voltage when required by a change in production schedule.

Some processes require that constant current be maintained through the electrolytic cells, even though the cell characteristics change frequently and rapidly. For these processes an autotransformer having a combination of no-load taps and tap changer under load can be supplied. The no-load taps cover the extreme voltage range required. The tap changer under load is arranged to boost or buck the d-c voltage from any no-load tap and in the range required. The tap points of the tap changer under load equipment can usually be made sufficiently fine so that phase control of voltage is not required between taps or, if used, will be limited to a few per cent in range. In many processes the fluctuations in voltage required to maintain constant current are so great and frequent as to require pro-

Figure 6. Elevation of a typical multiunit ignitron substation



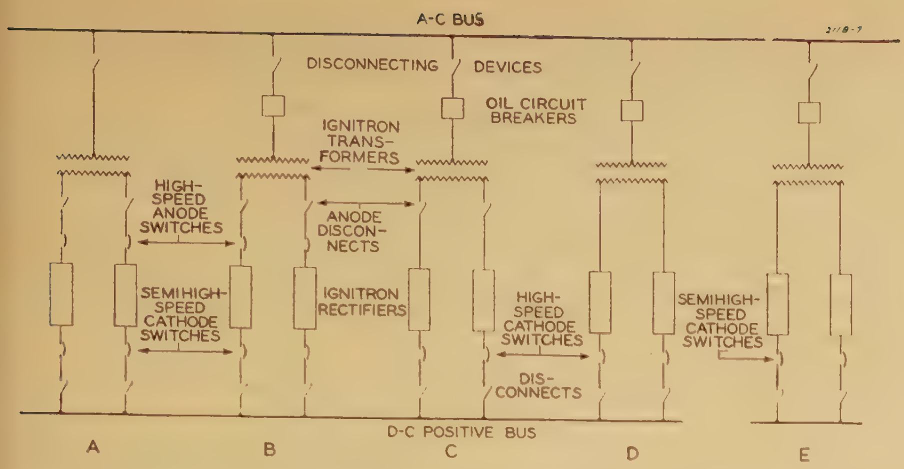


Figure 7. Schematic diagrams showing various arrangements of rectifier switching

hibitive frequency of tap changing or large phase-control angles. In these processes attempts to maintain constant current have been abandoned.

At some locations the demand clause in power contracts is such that high power rates must be paid over a year, because of a short-time swing over the demand ceiling. For processes where load conditions fluctuate rapidly and over considerable range, autotransformers with tap changer under load equipment can be used to secure constant kilowatt input to the cells. This will insure maximum production at the normal power rate, or at minimum power cost. Where more than one station is supplied from the same source, the averaging effect of the several stations proves most advantageous in reducing the number of tap changes required.

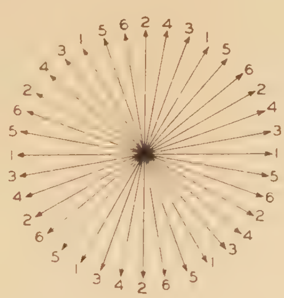
Regulation for either constant direct current or for constant kilowatt input can be made full automatic.

Figure 8 shows an autotransformer connected to the rectifier a-c bus, the voltage of which can be varied according to the electrolytic process requirements.

Since the loads on the large station busses constitute practically a short circuit for a single rectifier unit, it is not possible to place units on the bus individually at full voltage following a station outage. Where the power-supply system is large with respect to a single station, it has been general practice to connect all units to the power supply and the a-c bus without excitation, and then to close the excitation contactors to all units simultaneously with a master control switch. Where the supply system is not adequate for this procedure, station phase control of voltage is used. In this method of starting a station following an outage, all units are connected to the bus, either simultaneously as above or individually, but with phase-delay angles set uniformly to limit the station current to a value satisfactory to the supply system or to the rectifier unit, depending on the procedure. The phase-control angles are then advanced uniformly on all rectifier units to increase load to normal at a rate satisfactory to the supply system.

Telephone Interference

A rectifier of any type creates harmonics which are present in the a-c and d-c systems to which it is connected. Harmonics may be induced in telephone lines, which are adjacent to the power systems, under certain conditions of exposure. The telephone-interference problem becomes of increasing importance when the rectifier load is a large percent-



age of the system load in the exposure areas.

For electrolytic plants, the d-c load is concentrated, and exposures between the d-c circuits and telephone lines are easily controlled. When the a-c power is transmitted a considerable distance, or is supplied from a power network system, exposures are probable, and the telephone interference problem must be considered.

The number of phases at which any rectifier unit can operate cannot be greater than the number of anodes of the unit. Small units having only six anodes are therefore limited to six-phase operation, which operation is used as a base for rectifier harmonic consideration. Since the power involved in a six-anode unit is usually small, the influence on exposed telephone lines is usually too small to be important. As the number of phases in multiples of six are increased, some of the base harmonics of six-phase operation are cancelled, the cancelling increasing as the number of phases is increased. Operation of rectifier units in the higher multiphase relationships, therefore, decreases the telephone-interference problem.

A single rectifier unit of 12 anodes can be supplied from a transformer which is wound to give 12-phase operation. It is not economical to build a transformer for a higher number of phases in a single unit. For those installations which have a large number of rectifier units, multiphase operation can best be secured by operating the units out of phase with

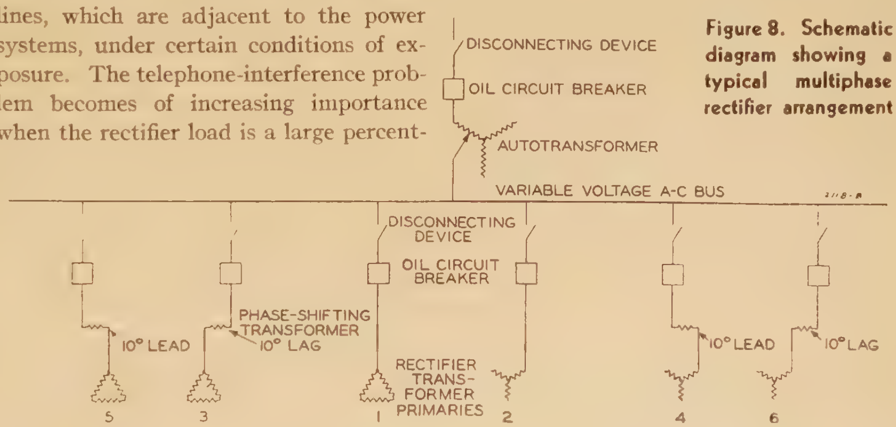


Figure 8. Schematic diagram showing a typical multiphase rectifier arrangement

respect to each other, and in symmetrical relationship.

The combination of two six-phase rectifier units having a delta and a star-connected transformer primary respectively will result in over-all 12-phase operation, since the delta and star are inherently 30 degrees out of phase. The supply voltage to a transformer can be shifted any number of degrees desired by the use of a phase-shifting transformer.[4-6] A combination of delta and star-connected transformers together with phase-shifting transformers can be used to effect any degree of multiphase relationship, the only limit being the number of rectifier units available. For phase arrangements greater than 12, the usual inductances in the circuits are sufficient to provide interphase action.

Table I

Number of Units in Substation	Anodes Per Unit	Base Unit Phases	Maximum Over-All Phase Operation Per Station
1	6	6	6
1	12 or 24	12	12
1	18	6	6
2	6 or 12 or 24	6	12
2	18	6	12
2	12 or 24	12	24
3	6 or 12 or 24	6	18
3	12 or 24	12	36
4	12 or 24	6	24
4	12 or 24	12	48
5	12 or 24	6	30
5	12 or 24	12	60
6	12 or 24	6	36
7	12 or 24	6	42
8	12 or 24	6	48

Figure 8 shows the circuits to the primaries of six rectifier units to secure 36-phase over-all plant operation.

To secure maximum benefit from multiphase operation, the units should be of equal rating, and their loads should be balanced. The impedance of the phase-shifting transformer will result in some unbalance in load between duplicate phase-shifted and unshifted rectifier units, unless some compensation is used. Compensating reactors can be connected in the circuits of the unshifted units, or transformer action can be built into the phase-shifting transformer which will compensate for its voltage drop at any given load.

The benefits secured from multiphase operation are not in direct proportion to the increase in the number of phases. The proportionate benefit from each succeeding increase in number of phases drops off sharply beyond 30- to 36-phase operation. Experience to date has demonstrated that 36-phase operation gives satisfactory operation from the telephone interference standpoint in most cases.

Recently the rectifier load on some power systems has become quite large and concentrated. Spot loading of three or more 36-phase operating groups may be encountered. For these special cases the sheer magnitude of the higher harmonies may be sufficient to warrant an increase in multiphase operation. Also a resonant condition for one or more of the higher harmonics may exist. For these installations operation at 72 or 108 phases may be of advantage.

If one rectifier unit, in a multiphase set-up, is taken out of service, an unsymmetrical phase relationship will result. Under this condition, a 36-phase arrangement gives just as good results as any higher-numbered phase arrangement. Operation with one unit out of service gives reasonably satisfactory results.

By the use of base units connected 6 or 12 phase, as required, and by phase shifting of associated units, the combinations shown in Table I can be secured.

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Synopsis: After preliminary development work systematic laboratory tests, closely simulating service conditions, were started in 1934 leading towards final development and improvement of gas-filled cable. The first full-size field installation of gas-filled cable was placed in service in 1938. The results of this work up to that time were summarized in an AIEE paper presented at the winter convention, January 1939. The present paper will deal with progress made since then, covering the whole range of voltage ratings from 10 kv to 138 kv, divided as follows: low-pressure gas-filled cable systems 10 kv to 40 kv, operating at gas pressures from 10 to 15 pounds per square inch; medium-pressure systems 40 kv to 69 kv, operating at gas pressures from 24 to 40 pounds per square inch; high-pressure systems 69 kv to 138 kv, operating at gas pressures from 150 to 200 pounds per square inch.

AFTER preliminary development work on gas-filled cable a systematic series of long-time load-cycle endurance tests closely simulating service conditions were started in our laboratories the early part of 1934, and this work is still being continued. These laboratory studies have proved a most useful guide in perfecting gas-filled cable design. They have allowed the best selection of materials and the most effective methods of construction and treatment in the factory.

The first full-sized field installation of gas-filled cable was placed in service during the summer of 1938, and since then quite a number of 15-kv and 27-kv installations have been added. Our first and only published report on gas-filled cable was in the form of an AIEE paper presented at the winter convention, January 1939.[1] It seems an appropriate time to summarize advancements made since then and co-ordinate with field experience.

Gas-filled cable has, we believe, technical merit and advantages that more than warrant its full development. It is simple, economical and of small size. Pressure control means uniform control of both insulation and sheath behavior with a lesser chance of trouble with either. The self-supervising feature is also important in view of the fact that most cable troubles come from sheath defects or damage. Finally, compound migration troubles are eliminated and there is no concern about the contour of the cable run within tensile strength limits of the cable itself. Three-conductor, 27-kv low-pressure cable has been in operation for several years past in vertical tunnel shafts from 200 to 250 feet in depth without trouble of any kind, nor has bleeding of compound from the insulation been any more than experienced in ordinary runs.

Three-Conductor Cable

The theory, characteristics, and design of gas-filled cable were dealt with in some detail in the previous paper and will not be repeated here. Briefly, this cable is similar in materials and construction to ordinary solid-type paper-insulated cable, with the exception that longitudinal gas feed channels are provided for uniform gas-pressure maintenance and control.

For low- and medium-pressure use at voltages up to 69 kv the three-conductor shielded type represents the most practical and economical form, since the gas feed channels can be conveniently located in the three triangular filler spaces that are otherwise merely waste space. Figure 1 gives a good idea of the cross-sectional construction. Two of these gas channels are of open steel spiral construction, giving free access to the insulated and shielded conductors. The third is a solid-wall metal tube filled with dry nitrogen gas and sealed off at each end before treatment of the cable length. The function of this tube after installation will be described later. Vacuum drying and impregnation treatment in a large tank before leading is exactly like that for solid-type cable. After impregnation and while still hot, the compound is drained from the tank, being displaced by dry nitrogen gas. The reel of cable is left in the heated tank until all surplus compound drains off, and only that compound held by capillary attraction in the dense cross section remains. The channels and outer surfaces present a clean appearance, but it is surprising how much of the heavy compound is held in the cable cross section by capillary balance, the finished cable being almost as well filled as a solid-type cable.

The treated reel length is then removed to a nitrogen-filled chamber directly behind the lead press, and the lead sheath is applied under a flow of nitrogen. After end sealing the gas pressure is raised to approximately ten pounds per square inch and the cable length is maintained at this pressure until ready for splicing in the field. Before the shipping reels leave the factory, the whole cable cross section is saturated with nitrogen to the same pressure as that in the gas feed channels. This assists greatly in maintaining uniform cross-sectional pressure in service.

Joints

Figure 2 shows a typical three-conductor joint. With exception of the tube insert connections and the fact that the lead casing is filled with nitrogen gas instead of compound, the joint is the same in construction and size as an ordinary solid-type cable joint, the same method of con-

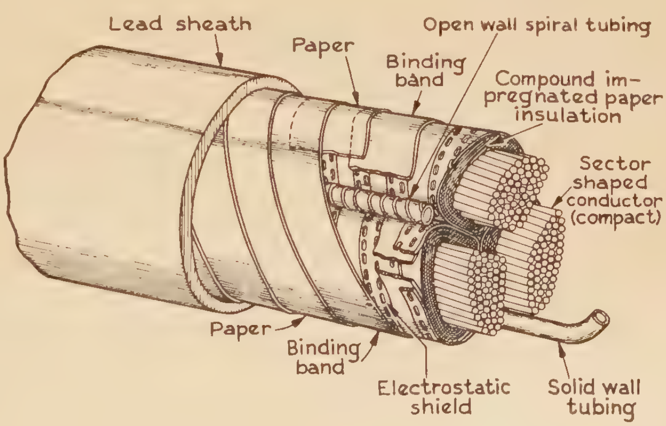

Figure 1. Three-conductor gas-filled cable

Cut-away view showing details of construction

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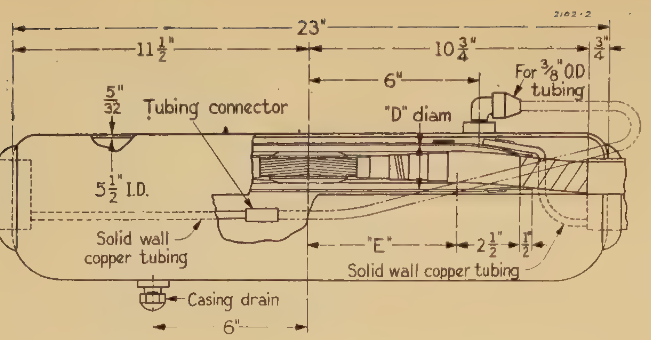


Figure 2. Normal joint

For 27-kv three-conductor gas-filled cable

necting, stepping, reinforcement taping, and shielding being followed. In preparation for splicing, the cable pressure is lowered to about two pounds, the ends are cut, conductors are spread, and temporary sponge rubber seals are applied in the crotch to prevent appreciable loss of gas during the remaining operations. The total time is the same as that required for constructing a corresponding solid-type joint, because of the fact that the time required for the special operations is compensated for by the absence of compound filling operations.

Pressure Control

The primary function of the solid-wall metal-tube insert in the cable is to furnish a by-pass path for gas flow at low points or dips in the cable run, where slugs or surplus compound might gradually form in the open channel spaces during initial periods of heavy load, or during hot summer weather. The method of interconnecting the tube ends and the joint casing, as shown in Figure 2, traps any compound that might enter the casing and assures a free gas path and uniform pressure control over the entire length of the cable line. The lead sleeve casing in Figure 2 is shown eccentric, the bottom part acting as a sump for collection of surplus compound. It has since been found that an ordinary concentric casing gives sufficient capacity for this purpose.

Field tests have shown that, with the help of this tube insert, pressure can be controlled at one terminal end only of lines up to about ten miles in length. A pressure relay at this one location will give an indication of gas leakage at any point in the line, and when loss of pressure occurs, additional gas can be injected from an emergency storage cylinder until the leak can be located and repaired. Where feasible, pressure control and relaying at both ends of long lines is desirable but not necessary.

The first installations of gas-filled cable, as described in the previous paper did not have the metal-tube insert. They are operating successfully, and there has not been any special trouble in maintaining and controlling pressure. It is necessary, however, or at least advisable, to "blow out" the cable line periodically with nitrogen gas and make sure the channels are sufficiently clear of compound to transmit gas pressure. The metal-tube by-pass insert has completely eliminated this need, and there is no question but that it simplifies and facilitates operation of three-conductor gas-filled cable.

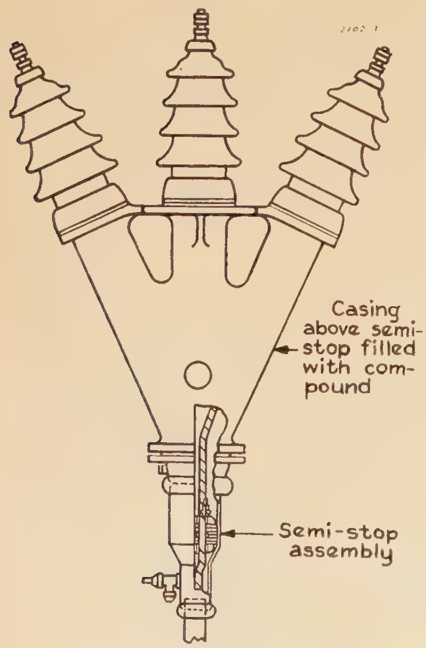


Figure 3. Compound-filled terminal

For 34.5-kv three-conductor gas-filled cable

Terminals

A typical soft compound-filled, three-conductor terminal for gas-filled cable is shown in Figure 3. The only departure from terminals used with solid-type cable is the semistop gasket assembly in the base to prevent the soft compound from draining into the cable channels. A simpler all-gas-filled terminal without the semistop has been developed for use at 15 kv and is also being made available for higher voltages. Both kinds of terminals are also designed as single-conductor and spreader three-conductor types. The only difference between terminals for low-pressure cable (10 to 15 pounds per square inch) and medium-pressure cable (25 to 40 pounds) is that heavier porcelains are required to withstand the higher gas pressure.

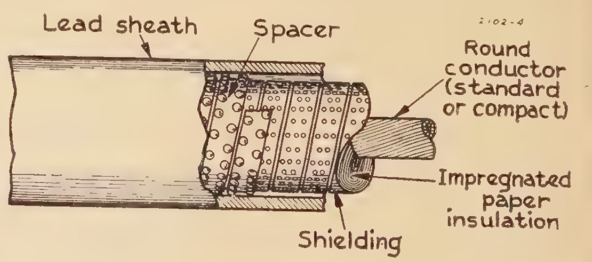
Single-Conductor Cable

The gas feed channel for single-conductor cable is in the form of an annular space directly under the lead sheath as shown in Figure 4. Copper tape with stamped button spacers centers the insulated shielded conductor and gives support to the lead sheath. In comparison, the single-conductor cable is less economical than the three-conductor type, because of the increased over-all diameter and cost this channel construction represents. In the three-conductor cable decreased insulation thickness and smaller diameter compensate for the extra gas operations in the factory and field, and the costs of low-pressure gas-filled cable and of solid types are practically the same. Single-conductor gas-filled cable, however, is of higher cost than equivalent solid type, because of the channel requirements mentioned.

This handicap is not very serious and in large sizes the low-pressure single-conductor type has found a useful place as primary station tie cable where numerous vertical runs are encountered. Absence of compound migration troubles and the self-supervision obtained from gas pressure control justify the extra cost. Another promising field for single-conductor gas-filled cable is for power transmission at 46 kv and above, when the load per circuit is too large or the ducts too small for three-conductor cable. In this voltage range medium-gas-pressure operation is more economical than low

Figure 4. Single-conductor gas-filled cable

Cut-away view showing details of construction



pressure, as will be discussed later, and the further reduction in insulation thickness gained thereby offsets the added cost of the gas channel construction.

The solid-wall tube insert for by-passing compound slugs, such as used in three-conductor cable, is not well adapted to single-conductor construction. This is largely overcome by paralleling the gas connections at each manhole of the three cables making up one circuit, assuring three separate paths for gas flow. The possibility of slug formation in all three cables at one time, in one location, and of sufficient number to materially interfere with pressure control, is remote.

Ionization as a Function of Gas Pressure

A fundamental and important characteristic of gas-filled cable is clearly brought out by a study of Figure 5, which shows ionization starting voltage as a function of gas pressure. A similar curve was given in the previous paper. At that time we had only limited test data above 15-pound pressure and little operating experience to confirm test results. Additional data obtained since allow a more accurate construction of this curve. It closely simulates operating conditions, in that it shows ionization voltage after capillary balance has been reached from load-cycle bleeding of compound, but before appreciable wax formation from test overvoltage has started.

The earlier paper described the self-extinguishing and self-healing properties of wax formation in voids of gas-filled cable. Further work has fully confirmed this important characteristic. It offers an additional factor of safety at those locations along a cable line where ionization might get started. Expressed in another way, gas pressure and ionization control automatically eliminate weak spots and result in more uniformity along the cable length. This in turn allows working voltage stress to more safely approach the critical voltage stress at which cumulative ionization deterioration occurs.

The whole question is whether the measured ionization starting voltage curve in Figure 5 represents this critical voltage stress or something less. From all of the evidence we have obtained it is the writer's opinion that the curve in Figure 5 is something less than critical stress and is actually a goal representing an upper limit of safe working stress in service to be approached as more experience is gained. There are sound reasons for believing this.

First, it has already been proved by numerous long-time load-cycle tests at pressures up to 30 pounds per square inch that gas-filled cable remains stable at stresses considerably higher than represented by the curve in Figure 5. Instability and ultimate breakdown on these tests occur at stresses from 40 to 70 per cent above the Figure 5 curve.

Admittedly, laboratory tests on relatively short lengths are not conclusive in establishing safe working stresses until confirmed by field experience. We have had confirming field experience of this kind with low-pressure gas-filled cable operating during recent years at an average gas pressure of 12 pounds per square inch and an average voltage stress of 65 volts per mil. Reference to Figure 5 will show that at this pressure ionization starts at 80 volts per mil. Accordingly, operating experience has been at a stress only 15 volts per mil less than this. No service failure has yet occurred in gas-filled cable systems and no signs of ionization have been detected. One length of 27-kv cable was removed for test after one year of service. It was found to be in perfect condition. This experience would indicate that there is a good margin of safety and some latitude for increasing present working voltage stress. It also indicates that uniformity in the field is under the same control as obtained in the laboratory on relatively short test lengths.

Additional proof that the initial ionization versus pressure curve in Figure 5 represents a stable and safe condition is found in the characteristic behavior of all representative lengths on long-time load-cycle tests. Typical test results of this kind are shown in Figures 6 and 7. Figure 6 is a chart of the whole test in terms of power-factor stability. Three load cycles per day from room temperature to 80 degrees centigrade were applied, and the gas pressure was maintained at ten pounds per square inch cold and not more than 14 pounds hot. It will be noted that the test was started at 85 volts per mil, only slightly above the ionization-pressure curve in Figure 5. After stabilization was assured, the voltage was increased, and these steps were repeated until final failure occurred after 16 days at 120 volts per mil.

Figure 7 gives power-factor voltage curves on this length as measured initially and at the end of each voltage step shown in Figure 6. Initial ionization at

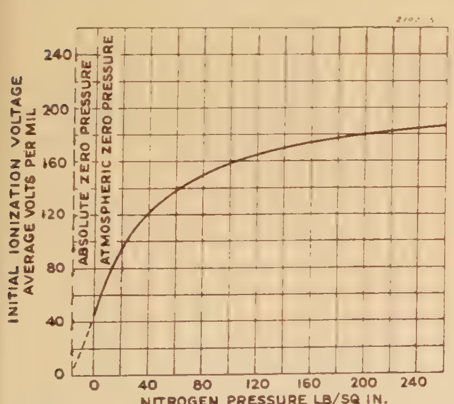


Figure 5. Ionization voltage versus gas pressure

Average voltage stress on solid insulation at which initial ionization starts in gas-filled cable after full drainage of compound but before endurance-test voltage is applied

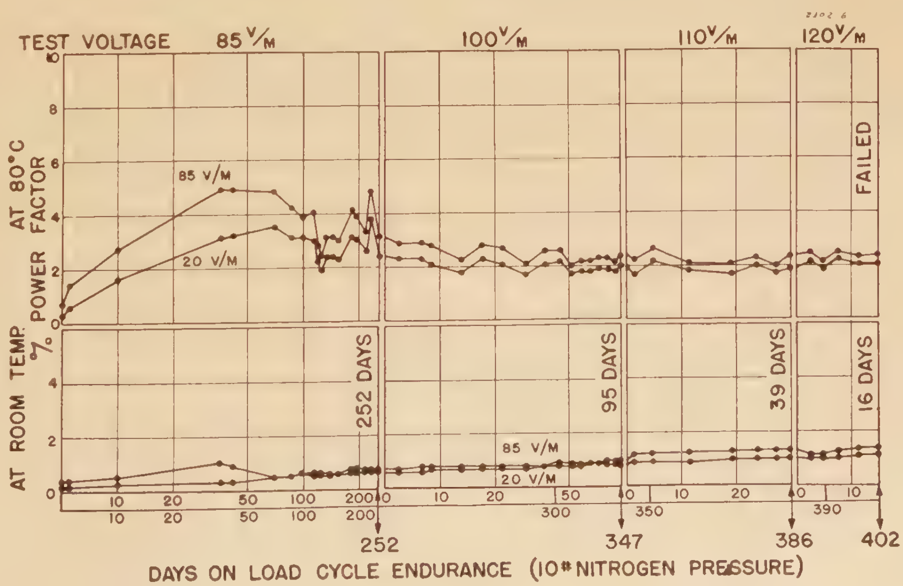


Figure 6. Load-cycle endurance at ten pounds pressure

Three-conductor gas-filled cable of standard construction, with exception that inner 50 per cent of insulation wall consists of three-mil paper tape instead of usual six-mil tape. Total insulation thickness 0.200 inch

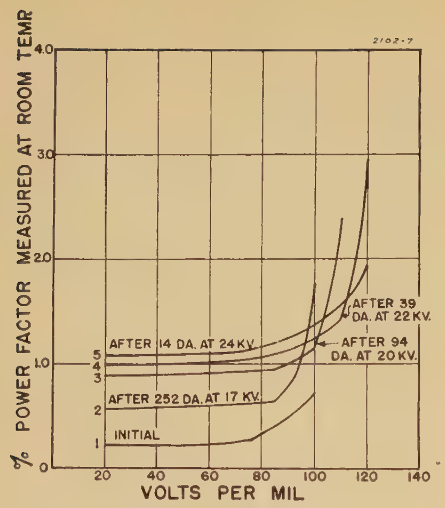


Figure 7. Ionization during load-cycle test

Showing increase in ionization starting voltage, due to wax formation, during different stages of the load-cycle endurance run charted in Figure 6

ten pounds pressure, and before wax formation started, was at 77 volts per mil (equivalent to 80 volts per mil at 12 pounds). Wax formation in the voids extinguished ionization, and the cable became stabilized in about 70 days, thereby automatically raising the ionization voltage to a value equivalent to endurance test voltage. This was repeated each voltage step until the last, 120 volts per mil. Here the stress was too high for the same kind of stabilization to take place and failure occurred in a few days.

If space would allow, additional evidence could be offered showing that the initial ionization-pressure curve in Figure 5 is a useful and practical guide in the design of gas-filled cable systems. Insulation thickness is an important economic factor in such design work. Figure 5 gives a direct indication of insulation thickness requirements as a function of working gas pressure. Increase in pressure means lesser insulation and a smaller cable. The higher the operating voltage, the greater the saving that can be made in this direction. There is an opposing economic factor however. An outer covering must be provided to withstand this gas pressure safely, and the higher the pressure the greater the cost of this covering.

Due consideration of these factors has led to a logical division of gas-filled cable design into three classifications or types, low-pressure, medium-pressure and high-pressure systems. Each covers a certain economic range of voltage ratings. All three have in common the general characteristics of gas-filled cable that have been described. Individual features, limitations, and advantages will be briefly outlined.

Low-Pressure Systems

The low-pressure system has already been described. Aside from comments on pressure control and leak location, only those features that distinguish it from medium- and high-pressure systems will be dealt with here.

Experience has shown that the normal gas capacity of the cable itself and the joint casings are sufficient to limit pressure fluctuations from daily load cycles to a range of only one to two pounds. It accordingly is not necessary to use automatic pressure-regulating equipment. Seasonal adjustment of pressure by hand to an average working value of 12 pounds per square inch is all that is necessary. An ordinary high-pressure storage cylinder of nitrogen is connected permanently to the cable line through a reducing valve. An exhaust valve is also provided. Normally these valves are closed. During routine inspection and for purposes of compensating for seasonal temperature changes, average working pressure is readjusted to 12 pounds by hand manipulation of these valves. If there is a loss of pressure in between these periods, a pressure relay gives notice when minimum pressure drops to ten pounds.

Leak location is not a difficult matter. It is first necessary to disconnect the external tube by-pass fittings on the joints (Figure 2) at intervals along the line and insert pressure drop reading devices. A graph plot of these readings indicates the point of leakage. The pressure-reading devices can be left permanently connected if desired, and one utility prefers this. Obviously, small slow leaks require a longer time and closer readings to locate than do fast leaks. It is entirely feasible to make these lines gas-tight initially and maintain this tightness without trouble.

It has already been shown that safe working voltage stress and insulation thickness are governed by working gas pressure. This, in turn, is determined by allowable hoop stresses in ordinary single lead sheath that will avoid sheath fatigue and hold creepage and expansion within negligible limits. The question of stress limits for lead sheath has been a subject of laboratory and field study for a good many years. A safe and conservative average working stress limit of 125 pounds per square inch of sheath cross section for copper bearing lead is well established.

Low-pressure gas-filled cable has the same standard sheath thicknesses as solid-type cable. On this basis and with a stress limit of 125 pounds an average working gas pressure of 12 pounds per square inch results. Present day insulation thicknesses for low-pressure gas-filled cable represent an average working voltage stress of from 65 to 68 volts per mil. Reference to Figure 5 will show this practice safe and conservative.

These design limitations of insulation and sheath properties and dimensions automatically set the economic voltage range. This range is found to be from 10 kv to 40 kv, and within these limits the cost of three-conductor low-pressure gas-filled systems is closely comparable to the cost of other available types of cable systems.

Above 40 kv, the proportionate saving in insulation thickness makes increased working gas pressure economical in spite of increased cost of the outer covering. This leads logically to medium-pressure systems for moderately high-voltage service.

Medium-Pressure Systems

In theory and principle there is no difference at all between low- and medium-pressure systems. Accessory design and methods of manufacture, installation and operation are the same for both types.

There are two available methods of sheath construction that allow increase of gas pressure:

(*a*). Reinforced sheath.

(*b*). Substitution of a special alloy sheath of greater creep strength than ordinary single sheath.

Both constructions are fully developed. The latter is economically the most promising, but both will be dealt with. It would be well, however, to first consider results of long-time load-cycle stability tests at medium pressure.

A typical test of this kind at 30 pounds pressure is charted in Figure 8. The three-conductor shielded-type cable used was of the same general construction and dimensions as that in Figure 6, having 350,000-centimeter compact sector conductors and 0.200-inch insulation. For comparative test purposes this size and a length of 75 feet were standardized for all of our gas-filled cable tests, the details of which are given in the previous paper. Standard compound was used for impregnation. Strand shielding with paper-backed aluminum tape was also used. Further tests with and without strand shielding will be discussed later, but it will be said here that, as far as load-cycle aging tests on gas-filled cable are concerned, strand shielding appears to im-

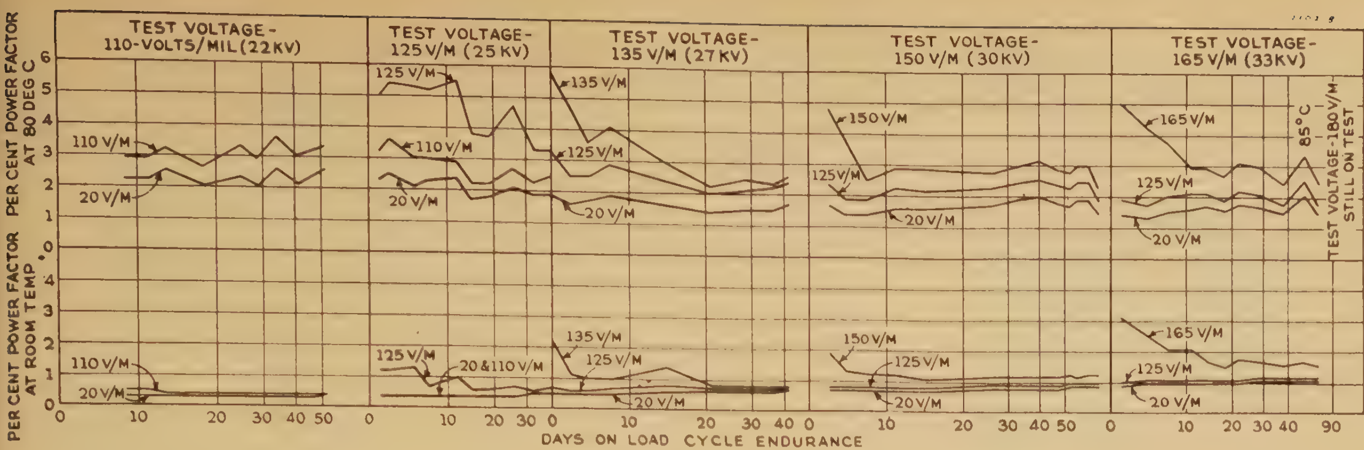


Figure 8. Load-cycle endurance at 30 pound pressure

Three-conductor gas-filled cable of standard construction, with exception of aluminum-paper strand shielding. Total insulation thickness 0.200-inch. No-load nitrogen gas pressure held at 30 pounds per square inch (medium pressure)

part no special benefit. This is due to the fact that voids, ionization, and wax formation are distributed uniformly in the butt spaces through the wall of insulation and do not concentrate at the conductor surface alone.

Referring to Figure 8, the 30-pound load-cycle test was started at 110 volts per mil. According to Figure 5, this is the initial ionization starting voltage at this pressure. There were practically no signs of ionization or instability at this stress, so after 50 days test voltage was increased to 125 volts per mil. Here, ionization was apparent but was extinguished after 15 days because of wax formation. As soon as there was assurance of stability (40 days) the test voltage was increased to 135 volts per mil. The same method of stepping voltage was followed to the end of the chart, and it will be noted that self-healing action and stability were retained at 165 volts per mil. This length is still on test at the present writing at 180 volts per mil without signs of failure.

It might be concluded from this test that an increase in gas pressure from 10 to 30 pounds imparts more benefit than would be indicated from the initial ionization curve in Figure 5. However, it appears sounder to use Figure 5 as a guide and not depend too greatly on self-healing action in service, even though it does appear more pronounced as gas pressure is increased. On this basis we believe that an operating stress in the order of 95 volts per mil is safe and conservative at 30 pounds pressure.

Double Reinforced Sheath

Double reinforced sheath was developed originally for oil-filled cable and has been used extensively for this purpose. There have been a good many years of experience at pressures from 30 to 40 pounds per square inch. Based on this, we believe 40 pounds a safe top limit and have designed medium-pressure gas-filled cable with this type of sheath for an average working pressure of 35 pounds and an operating voltage stress of 100 volts per mil. The economic voltage range is from 40 kv to 69 kv.

The double reinforced sheath consists of treated paper and copper tape reinforcement, sandwiched between inner and outer sheaths. The cost is higher than that of ordinary single sheath. Insulation reduction from increased gas pressure does not fully compensate for the increased cost of sheath. Accordingly, the cost of this type of three-conductor medium-pressure cable is higher than that of equivalent single-sheath oil-filled cable. Accessories however are simpler and less costly, as are also installation operations in the field. The result is that over-all costs of the two types of cable systems are not far apart.

High-Creep-Strength Alloy Sheath

From the beginning we have appreciated the economic possibilities of substituting an alloy sheath of higher creep strength than ordinary copper-bearing lead sheath. The requirements are exacting and proved difficult to attain. Such an alloy must retain all of the desirable and proved properties of copper-bearing lead, plus a sufficient increase in long-time creep strength to allow a real increase in working gas pressure. The requirements to be met are:

(*a*). At least twice the long-time creep strength of copper-bearing lead at operating temperatures.

(*b*). A reliable source of supply at reasonable cost.

(*c*). Readily adapted to sheath-extrusion methods in the factory.

(*d*). Equally resistant to corrosion, vibration, bending movement, and aging.

Out of a number of possible lead alloys investigated in recent years the calcium alloys of lead are the most promising, and one in particular, Asarco, will be dealt with. Figure 9 shows the long-time room-temperature creep strength in comparison with that of copper-bearing lead. Similar tests at 100 degrees centigrade plainly show that increasing the average working stress from 125 pounds per square inch for copper-bearing sheath to 250 pounds for Asarco sheath is conservative.

This means that with present standard thicknesses of single lead sheath we can, by using such an alloy, safely increase working gas pressure to 24 pounds per square inch, and corresponding voltage stress to 90 volts per mil, a gain of 38.5 per cent in voltage rating as compared with low-pressure systems operating at 12 pounds pressure. For the same voltage rating the corresponding reduction in insulation thickness is 27.5 per cent.

Regardless of whether the object is to increase voltage rating or to decrease insulation thickness for a fixed voltage rating, this theoretical gain is more or less offset by the increased costs of the special alloy sheath, and the lower the voltage rating the more pronounced this offset becomes. Because of these conflicting cost factors, this particular type of medium-pressure system, like the double reinforced sheath system previously described, has an economic voltage range from 40 kv to 69 kv. Below 40 kv the saving practically disappears, and much above 69 kv, such as 115 kv for instance, three-conductor cable becomes too large to be economical. There is some promise, however, in single-conductor cable above 69 kv. Here the extra cost of the gas-channel construction and the alloy sheath

are offset by the other savings described. All told, we consider this simplified medium-pressure gas-filled cable system an important technical and economic advancement.

High-Pressure Systems

Full advantage of high-gas-pressure operation can only be realized by drawing the cable into a welded steel pipe line. There have been proposals to use a special double lead sheath construction, reinforced for both radial and longitudinal strains, to operate at gas pressures as high as 200 pounds per square inch. This type of cable could be installed in underground ducts in the usual manner which is an advantage, but there appear to be two serious obstacles.

1. The over-all cost is too high in comparison with other available cable systems, such as oil-filled, medium-pressure gas-filled, and, finally, high-pressure gas-filled drawn into steel pipe.

2. There is serious doubt of the feasibility of safely operating reinforced sheath at such high pressures, because of mechanical strength limitations.

There is no doubt but that the steel pipe construction is the most promising, and further comments will be confined to that type.

When properly designed and installed, the high-pressure gas-filled pipe cable system is technically sound and economically attractive. It has however certain limitations that should be fully understood.

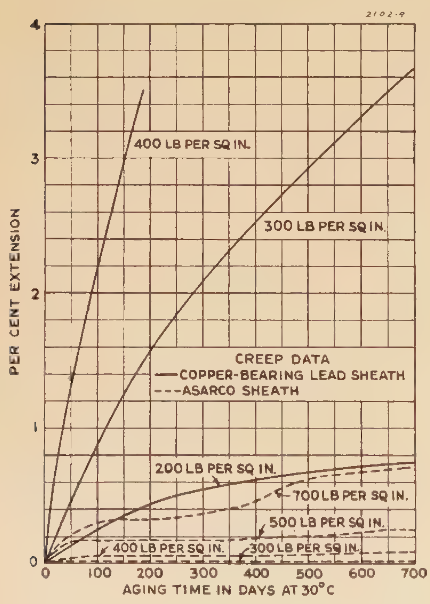


Figure 9. Creep properties of lead sheath

Long-time room-temperature creep properties of Asarco and copper-bearing lead sheaths. Tests started two weeks after extrusion

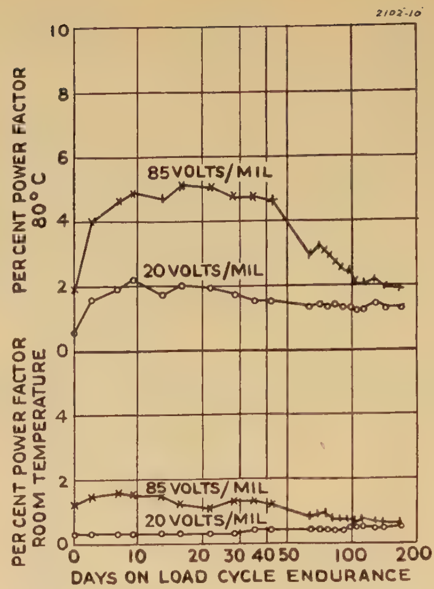


Figure 10. Load-cycle endurance at ten pounds pressure

Standard three-conductor gas-filled cable with aluminum-paper strand shielding similar to cable in Figure 8. No-load nitrogen gas pressure held at ten pounds per square inch (low pressure). Cable still on test at 100 volts per mil

1. It is best adapted to direct burial in open ground, and its use is likely to be limited in this country where most underground systems are in congested areas under paved city streets. The standard duct and manhole construction (and the flexibility it represents) has always proved superior to direct burial under these conditions.

2. Important high-voltage tie-line loads in this country are growing, and the steel pipe cable system is not well adapted to handle extra heavy loads. It is not possible to use one single-conductor cable per pipe, with three pipes spread out to better dissipate copper losses. Instead, it is necessary to draw all three cables into one congested pipe, and the losses to be dissipated are much higher. In this respect pipe cable is no different from other forms of three-conductor cable and has the same well-recognized limitations for economic loading.

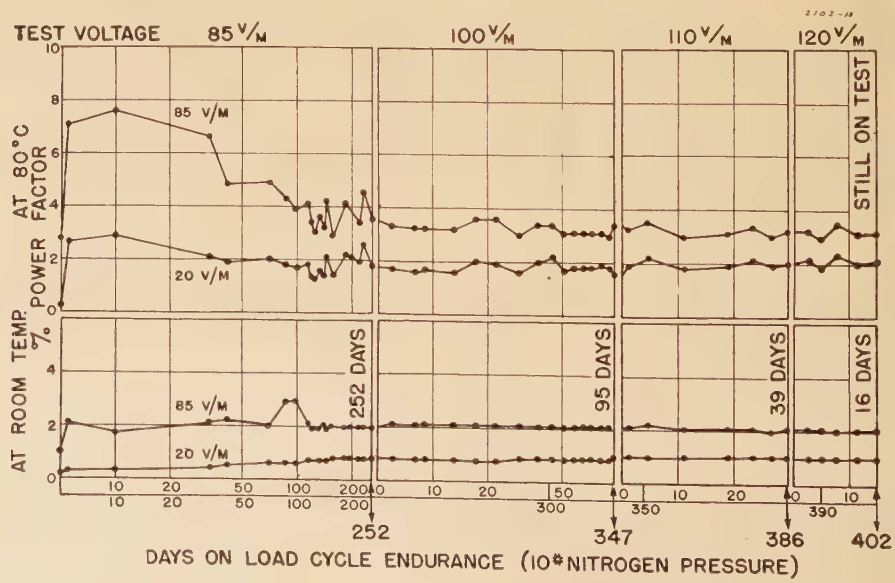
These aspects of the problem are not so important in Europe where the practice of direct burial in open ground is commonly followed, and where load requirements per circuit average considerably less than in this country.

From an insulation standpoint gas-filled pipe cable offers no difficulties. Reference to Figure 5 will show that at 200 pounds pressure initial ionization is at 180 volts per mil. In view of greater self-healing properties from wax formation at high pressures, we could probably operate with safety at an average stress not less than 160 volts per mil. In this respect high-pressure gas-filled cable, like oil-filled cable, has greater 60-cycle strength than can be utilized. Insulation thicknesses for oil-filled cables are based on transient voltage requirements rather than normal 60-cycle rated voltage stress, and the same thing should hold for high-pressure cable. Accordingly, insulation thicknesses for high-pressure gas-filled cables are the same as for oil-filled cable, the rated voltage stress varying from 126.5 volts per mil at 69 kv to 142.5 volts per mil at 138 kv.

Figure 5 shows that this range of voltage stresses is conservative at 200 pounds pressure. In fact, the gas pressure could be reduced to 125 pounds without danger. There is no particular object, however, in not taking advantage of the higher factor of safety offered by the higher pressure, since standard steel pipe will withstand

Figure 11. Load-cycle endurance at ten pounds pressure

Standard three-conductor gas-filled cable exposed to room air for eight hours before applying the lead sheath. Chart shows the effects of oxygen absorption during exposure



200 pounds pressure just about as well as 125 pounds.

Details of pipe construction, welding, covering for corrosion protection, joint sleeves, compressed air test for leakage, and so forth will not be dealt with here, since this practice is well-known and common to all of the various pipe cable systems, of which there is a number of installations in Europe and a few in this country.

Sheaths Intact

After the pipe is laid and ready to receive the gas-filled cable, the remaining operations vary, depending upon whether the sheaths on the three single-conductor cables are left intact or stripped off as the cable lengths are drawn into the pipe. Each of these two methods has advantages and disadvantages that need careful balancing. For loads involving conductor sizes above approximately 750,000 circular mils, short-circuited sheath losses become so great that there is not much choice, economy dictating removal of the sheaths. For lighter loads and smaller conductor sizes there is much to be said in favor of leaving the sheaths intact, but it is difficult to draw an exact dividing line between these two practices without studying each specific case.

With sheaths intact the methods of manufacture, shipment, installation, and splicing are exactly the same as already described for low-pressure systems in ducts, the only difference being that the gas channel between the insulated shielded conductor and the sheath is smaller and simpler than shown in Figure 4, an open spiral wrapping of fibrous tape being substituted for the metal button spacer tape. Each joint is enclosed in a wiped lead sleeve, and since splicing is done under a slow flow of nitrogen gas, the cable insulation is at no time exposed to air or moisture. It is not necessary to remove these impurities from the pipe until the whole line is completed. At that time the pipe can be dried out by short-circuit current in the conductors, or circulation of hot air, or both. Afterwards, the dry air in the pipe is washed out by nitrogen flow assisted, if desired, by drawing a rough vacuum first. These same operations can be done piecemeal on sections of the line as completed if more convenient, and the finished sections maintained at a few pounds nitrogen pressure.

After the whole line is ready, the outlet plugs in all lead joint sleeves are removed, the plugs in the outer steel sleeves replaced, and the gas pressure is brought up to 200 pounds. There is then an interconnection of gas pressure between cable and pipe at every joint sleeve. Equalized pressure throughout removes any question of stress burden on the lead sheaths.

If future repair work, involving replacement of cable lengths, is ever required, these operations are reversed. After lowering the gas pressure, outlet plugs in the lead joint sleeves are replaced, segregating the cable from the pipe. Replacement of cable can then proceed as with low-pressure cable, and at no time is the insulation exposed to moisture and air absorption.

Sheaths Stripped Off

In addition to elimination of sheath losses and reduction in conductor size obtained thereby, stripping of sheaths as the cables are drawn into the pipe has one other advantage. The removed lead can be salvaged. The disadvantages are:

(*a*). An outer tape wrapping and copper armor are necessary for protection during pulling.

(*b*). Extra cost and time for stripping the sheath and protecting cable ends from exposure.

(*c*). Pipe must be dried and filled with nitrogen gas piecemeal before the cable is pulled in, and pulling must be done under a slow flow of nitrogen.

(*d*). Absorption of air and moisture during unavoidable exposure will increase dielectric losses and require a heavier wall of insulation than when sheath is left intact.

(*e*). Future repair work without undue exposure of insulation is more difficult and complicated.

Comparative over-all installed costs and service records will be required before the relative merits of these two high-pressure gas-filled cable systems can be finally decided in those cases where sheath losses are not a dominating factor.

Summary of Recent Test Results

Space will not allow a detailed presentation of all the test data obtained on gas-filled cable since the last published report. We have carried out comparative test studies of the best available impregnating compounds, particularly as a function of viscosity, the effects of paper thickness, strand shielding, exposure to air, further tests on the effects of gas pressure, and some impulse tests. Conclusions drawn from these tests will be summarized, and pertinent results relating to these conclusions given.

Thickness of Paper Tape

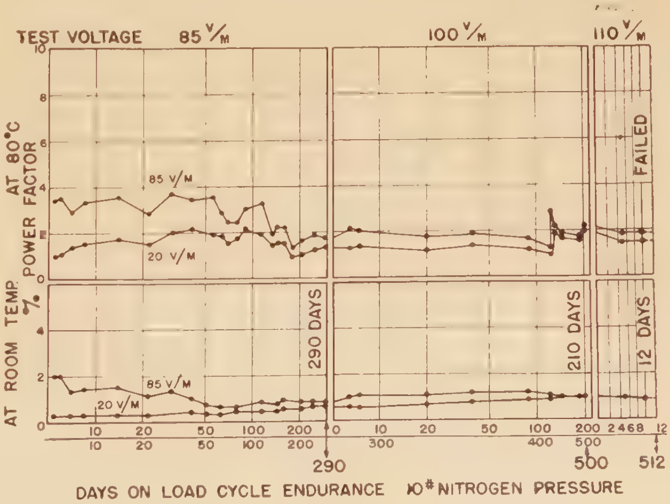
The load-cycle test in Figure 6 represents a test length of cable with the inner half of the insulation composed of three-mil paper tape instead of the usual six-mil tape. In all other respects this length was of standard construction. Theoretically, thinner tape means smaller butt space voids and higher ionization voltage. Actually, Figure 7 shows that ionization voltage was no higher than usually obtained with standard six-mil tape. Standard cable retains stability up to 100 volts per mil on load-cycle test at ten pounds pressure and ultimately fails at 110 to 120 volts per mil. The thin paper length in Figure 6 lasted little if any longer, failing in 16 days at 120 volts per mil. In view of the fact that thin tape is more expensive and difficult to apply, tends to wrinkle more, and will not withstand cable bending as well, it is doubtful that there is any benefit that overbalances these disadvantages.

Strand Shielding

It has already been explained that the test length represented in Figure 8 had strand shielding, and the test results did

Figure 12. Load-cycle endurance at ten pounds pressure

Three-conductor gas-filled cable of standard construction, with exception that viscosity of impregnating compound was increased to 350 Saybolt at 100 degrees centigrade by addition of 1.5 per cent soluble synthetic compound



not appear to show any particular improvement in ionization characteristics over equivalent cable without strand shielding. This is confirmed by load-cycle tests on an additional strand-shielded length as given in Figure 10. Ionization starting voltage is no different, and it required about the same length of time (75 days) to stabilize at 85 volts per mil as experienced with standard lengths without strand shielding. The length in Figure 10 is still on test at increased voltage stress. Possibly ultimate results will show some benefit from strand shielding, but we haven't found it yet. The explanation seems to be that ionization and wax formation occur more or less uniformly in butt spaces throughout the insulation thickness and do not concentrate at the conductor surface alone.

Exposure to Air

To study the effects of casual exposure to air, as may happen in jointing operations or through some accidental exposure, a standard test length was exposed to room air for eight hours after removal from the treating tank and before the lead sheath was applied. The results of load-cycle testing are given in Figure 11. It will be noticed that oxidation action from the absorbed air increased dielectric power factor at both room temperature and 80 degrees centigrade, the latter increasing to about double that met with in standard cable. It will also be noticed that in spite of this the cable had good endurance and was removed from test after 16 days at 120 volts per mil without failure. The conclusion can be drawn that, within accumulative heating limits, oxygen absorption does not affect voltage endurance but does appreciably increase dielectric loss and should be avoided for this reason. This is particularly true of medium- and high-pressure systems operating at the higher voltage ratings, where liberties with impurities and dielectric loss cannot be taken without serious risk.

Effects of Compound Viscosity

The series of tests we have carried on over a period of years on gas-filled cable indicate rather definitely that there is such a thing as optimum viscosity of impregnating compound and that this is in the order of 100 Saybolt at 100 degrees centigrade, representing the standard all-mineral compound used for some time past. We invariably obtain better results with this compound than with either thinner or heavier impregnants, apparently for reasons explained in the previous paper.

Check tests, that will not be given, again show that thin oil, such as used in oil-filled cable and having a viscosity of 37 Saybolt at 100 degrees centigrade, drains too freely from the insulation and results in poor ionization and endurance characteristics. This is not surprising and would be expected from theoretical considerations. The surprising thing is that various high-viscosity compounds give from fair to very poor results, but even the best shows no advantage over the standard compound.

We have tried about all of the recognized available types of high-viscosity compounds, including rosin mixtures, soluble synthetic mixtures, and hydrogenated oil, with viscosities from 200 to more than 1,000 Saybolt at 100 degrees centigrade. All of these, with one exception, gave poor and unstable results that are hardly worth showing. The best consisted of standard compound mixed with 1.5 per cent soluble synthetic to give a viscosity of 350 Saybolt at 100 degrees centigrade. An incomplete test on a length impregnated with this compound and designated as test length 8, was reported in the previous paper. The complete test is charted in Figure 12. The results obtained from both an ionization and voltage-endurance standpoint were about an average of those obtained with standard cable.

Since extra high-viscosity compound means longer periods of impregnation treatment and drainage, and more difficulty in clearing out gas feed channels, we have, as yet, found no advantage that would justify its use. A study is also being made of preimpregnated tape and, so far, no advantages have been found that would justify its complications and greater cost. Further and more complete tests may modify this belief.

Impulse Tests

After 21 days of vertical drainage with 75-degree-centigrade load-cycles five lengths of single-conductor, 1,000,000-circular-mils gas-filled cable, without strand shielding and having 0.203-inch insulation, were subjected to standard impulse breakdown at a gas pressure of ten pounds per square inch. An average breakdown of 367 kv was obtained with a deviation of 35 kv, or 9.6 per cent. This corresponds to an average stress of 1,800 volts per mil, compared with an average of 1,680 volts per mil obtained on 33 lengths of ordinary solid and oil-filled cable. On the basis of maximum stresses, which some prefer, the gas-filled cable gives 2,130 volts per mil, and the solid and oil-filled, 2,260 volts per mil. For all practical purposes it can be said that the results on all three types are closely the same. It is reasonable to assume that the same thing would hold for medium- and high-pressure gas-filled cable, since the insulation thickness is not under compression and does not vary with pressure.

Reference

1. Low-Gas-Pressure Cable, G. B. Shanklin. AIEE Transactions, volume 58, 1939, July section, pages 307–18.

Improvements in Preventive-Coil Control for A-C Locomotives With Particular Reference to Resistor Transition

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SINCE the original development of the a-c locomotive there have been many attempts to improve the locomotive control over that provided on early designs.

Control problems have come under three general classifications, namely:

1. Voltage control for the acceleration of the locomotive.

2. Functional control for adjusting various fields of the series traction motor for best commutating characteristics.

3. Protective relays for protection of locomotive and traction motor circuits from grounds and faults of various kinds and for protection of the traction motor from overspeeding.[1,3]

This article will deal only with the first of these problems, namely, those incident to the voltage control as it relates to accelerating a locomotive, with particular reference to a new development in the art. The new development is the resistor transition scheme for switching preventive coils from one pair of transformer taps to the next. A résumé of service experience with a locomotive so equipped is also included.

While the search for a better method of acceleration has proceeded, there have been no revolutionary improvements made, and progress has been essentially a refinement in design.

Early locomotives made use of a switching reactor, or preventive coil, in tapping from one set of transformer taps to the next, and the scheme which developed the most favor soon grew to what was termed as the three-preventive-coil notching scheme. In this scheme two small preventive coils feed a large preventive coil which in turn feeds the traction motor circuits through its mid-tap. The two small preventive coils are connected to two sets of bus bars, which are connected by tap switches to the main transformer taps in the correct order. Each tap switch carries one quarter of the load current in the normal design where all preventive coils have their mid-tap equidistant from the two end terminals. The three-preventive-coil scheme of control has persisted and is now the scheme most generally used on a-c locomotives in the United States.

During recent years several voltage-control schemes of a somewhat different nature have been devised and in some instances have been tried out in service both in this country and abroad, but by and large the schemes have met with indifferent success. Schemes tried in this country include one that has used a relatively large buck-boost transformer, the primary of which is controlled by means of light-current relatively high-voltage switches. When the large buck-boost transformer has been notched up to its maximum voltage, balanced voltage conditions are obtained. This condition is retained through suitable transfer switches while the buck-boost transformer is reconnected to repeat its operation from higher main taps on the main transformer. Also there have been tried variations of European schemes, utilizing a motor-driven switching device which shifts taps under zero voltage conditions obtained by mechanical interlocking with arcing switches designed to handle the arcing duty.

Since the year 1937 improvements in the basic type of voltage control have been evolved which have consisted essentially of improvements in the contactors themselves, in the interlocks which are used to insure against faulty closure of switches, and further in an arrangement of auxiliary relays known as "sequence or interlocking" relays which, while having increased the number of operating coils in the system, have materially decreased the number of contacts in the control circuits and have given an overall improvement in reliability and a decrease in maintenance.[3]

One of the chief sources of trouble that arises with notching schemes using individual contactors is the failure of one of the individual contactors to open its circuit when it is called upon to do so. This may arise from purely mechanical reasons such as sticking of the operating rod or some of the mechanical portions of the contactor, or from electrical reasons, such as those which cause the tips of the contactor to weld together. Because of the relatively large number of tapping contactors used upon a modern locomotive (24 in the case of the latest New Haven and Pennsylvania Railroad locomotives), the number of interlocks required for complete protection against contactor closure when a given contactor has stuck closed for some reason was prohibitive. For this reason previous to 1937 it was considered sufficient to interlock positively against direct short circuits upon a given preventive-coil bus, and no attempt was made to prevent possibilities of overvoltage being applied to a preventive coil.

The development of the "sequence relay" made interlocking possible which would prevent the closure of any switch that might set up undesirable conditions after another had stuck closed.

To protect against an unbalanced preventive coil, caused by failure of a switch to close in sequence, differentially connected current transformers supplying a thermal-element preventive-coil relay are provided. The relay characteristics are so proportioned that on normal switching operations, in which one tap switch is opened before the next is closed, when the preventive coil is left connected with but one leg for a few cycles, the relay will not trip. Should a tap switch remain open, the heater element will cause the relay to trip after a definite time, opening the control circuits to all the tap switches. Such thermal relay connections also provide protection against internal failures of the small preventive coils, as such faults give rise to current in the same direction as exciting currents and thus are effective in tripping the thermal element.

Operating experience prior to 1937 developed the weakness of utilizing the three possible unbalanced preventive-coil combinations as the first three starting notches for a locomotive. Locomotives so connected gave rise to an abnormal number of preventive-coil failures directly traceable to operation on one of the unbalanced notches while starting heavy trains at difficult locations. Modern practice therefore prescribes the use of balanced connections for the coils for all notches—except possibly the first—

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where currents are low, and there is no occasion for extended operation.

The aforementioned improvements, however, do not progress greatly toward the solution of one of the first criticisms of the a-c locomotive, namely, the sag in tractive effort that occurs between notches. During the transfer from one notch to the next it is necessary that one end of a preventive coil be open-circuited completely before closing it in on the next higher voltage. During this time the total load current for the particular preventive coil is carried through one half of its winding. This winding offers a considerable impedance to the load current because of the unbalanced ampere turns in the coil. The impedance drop under these conditions becomes the impedance that will be supported by the iron plus the series-air-core impedance of the turns in the coil itself. As used here, "series-air-core impedance" refers to the reactance of the coil, assuming no iron is present, as the ampere turns available are many times in excess of those required to saturate the iron.

The insertion of the high impedance of one half of a preventive coil in the circuit causes a very marked decrease in the voltage left to be applied to the traction motors, with the result that there is a considerable falling off of traction motor current, and thus a sag occurs in the motor tractive effort. Under severe conditions of starting and running when operating at tractive efforts very close to the slipping point of the wheels, such a reaction during the notching is very undesirable. Most of the control schemes tried out or proposed have had as one of their main objectives the smoothing out of the notching characteristics between notches. Some of the schemes have been more or less successful in this, but for the most part they have introduced other undesirable features.

Another characteristic inherent in the three-preventive-coil scheme is the presence of the extremely high voltages which occur in the saturated preventive coil undergoing tap change. Such voltages are of the "peaked-wave" variety, maintaining for only a very small portion of each cycle and occurring when the current wave passes through zero. At the higher transformer voltages and motor currents the "peaked-wave" voltages have been measured which reach values of approximately twice the transformer secondary voltage. This comes about from the fact that all of the available voltage of the transformer secondary may appear across one half of the small preventive coil for an instant during each cycle, and the turn ratio of the preventive coil will give twice this voltage between its two outside terminals. When it is considered that the nominal rated voltage of a small preventive coil is of the order of 128 volts, and the secondary voltage of the main transformer may be of the order of 1,400 volts, the amount of overvoltage that is applied to a preventive coil is extreme.

The presence of peak voltages during switching operation imposes a very severe switching duty upon the tap switches, as is evidenced by the loud noise and substantial arcing that usually occurs during operation of the tap switches under load conditions.

The saturation of the preventive coil during the transition period has another effect which at times in the past has been the most important phenomenon in connection with the switching. This is the occurrence of a displaced current wave upon the closure of the next higher tap switch with the result that very high values of transient currents are obtained. If the value of air-core reactance in the preventive coil undergoing switching is not of sufficient magnitude, currents high enough to cause welding of the tap switch may occur.

These extremely high currents come from:

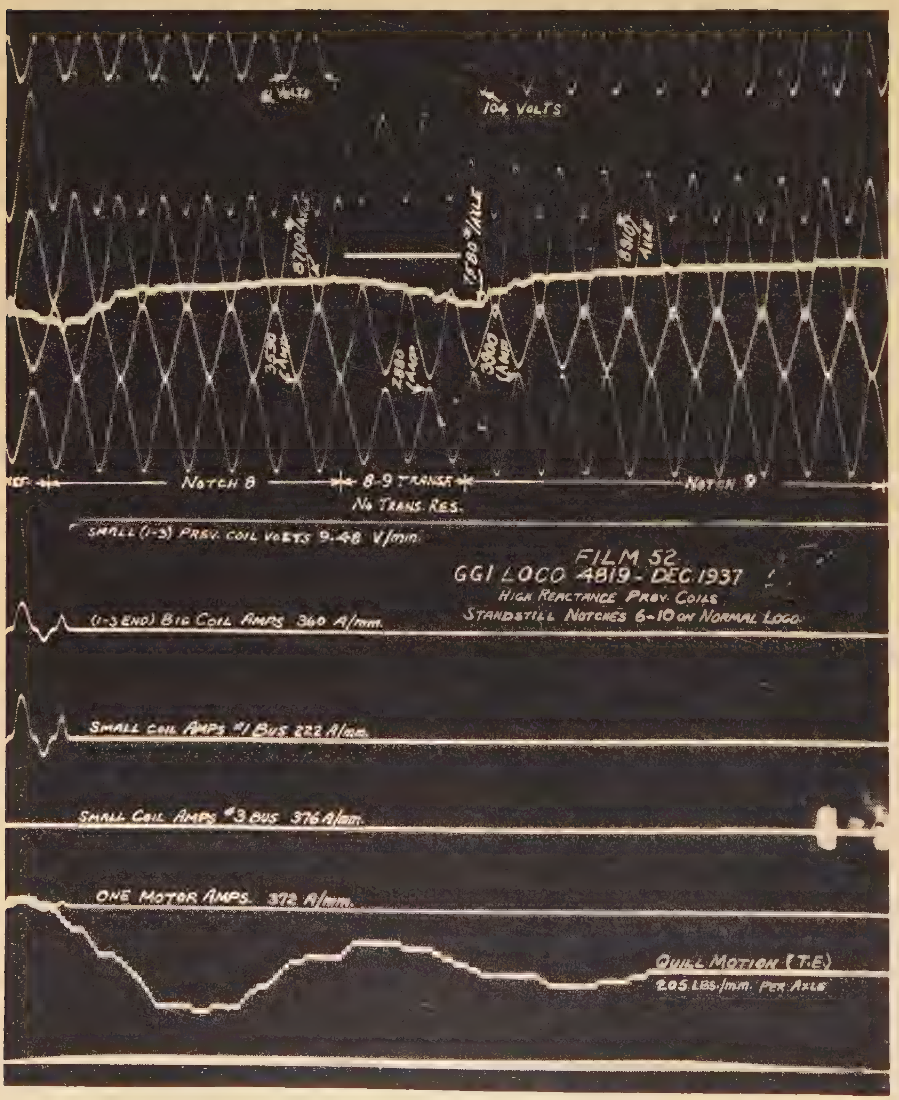
1. Trapped flux in the coil being switched due to the reversal of applied voltage. This phenomena is similar to that which sometimes occurs when a power transformer is energized and the switch closure comes when the instantaneous applied voltage does not correspond to the flux in the transformer iron, with the result that a distinct "bump" can be heard if one is in the immediate vicinity of the transformer.

2. A component from the trapped flux of the large preventive coil which may be saturated, and many times is, during the switching operation.

3. The proportionate share of the load current for the particular switch involved.

Such currents do not occur every time a transition is made, as their occurrence is dependent upon the instant during the

Figure 1. Film 52—transition between notches 8 and 9 without transition resistor



current wave that the tap switch is closed. For this reason tests designed to record the maximum values of current obtained upon contactor closure have to be made recording the results of very large numbers of closures.

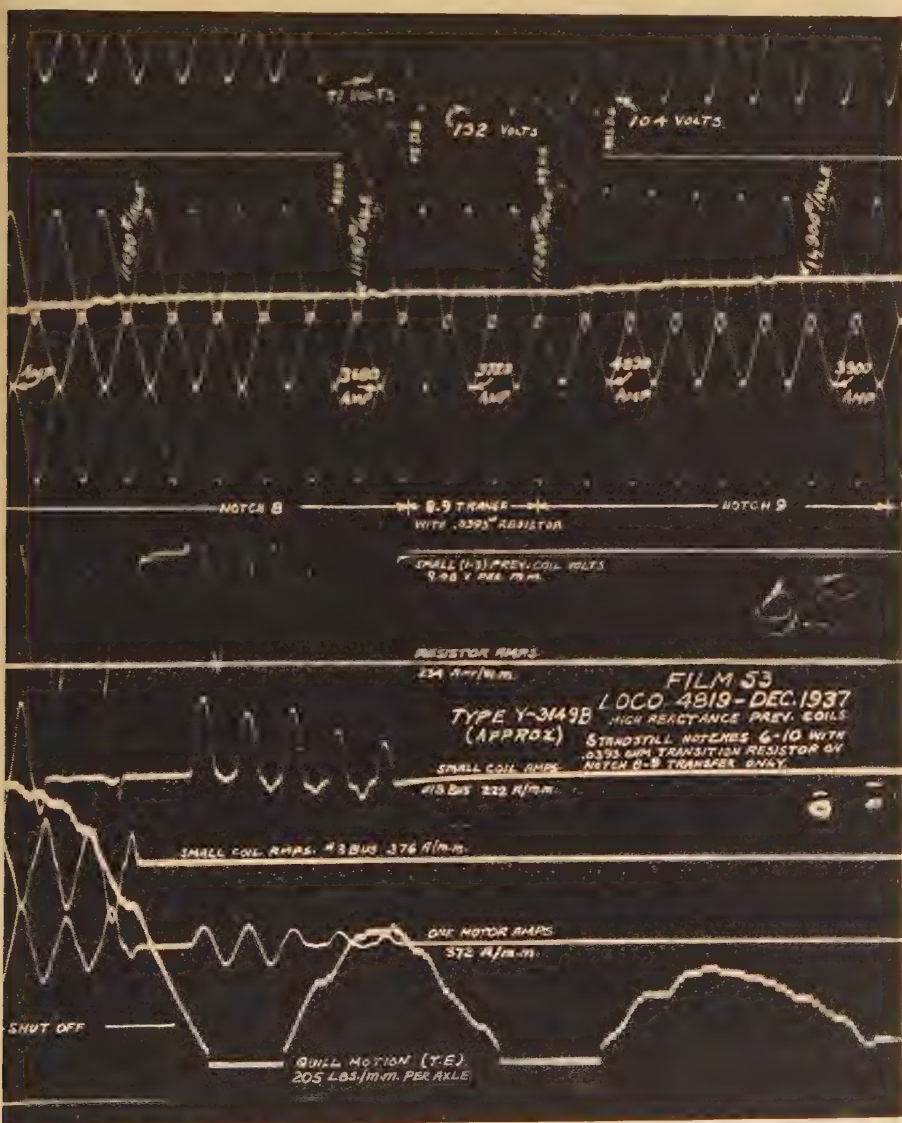
The surge current peaks have been measured as high as 23,000 amperes in a 1,250-ampere contactor, and possible values may be calculated with a reasonable degree of accuracy. Preventive-coil air-core reactances are usually figured to limit these currents to around 12,000 to 14,000 amperes, which seems to be a satisfactory value for the circuits and contactors involved.

Most of the early difficulties with the three-preventive-coil scheme of control centered around the welding of the tap-switch contacts, as then it was not known what magnitude in surge currents would be obtained, nor what steps would have to be taken to limit these currents to satisfactory values.

One of the suggestions made during the early days involved the connection of a resistor across the preventive coil to prevent the extreme amount of saturation which occurs in the coil. However, this suggestion was not followed up as it appeared simpler and more helpful to connect special air-core reactances in series with the preventive-coil bus circuit to limit the flow of exciting current through the small preventive coils. While the air-core reactance coils were so connected as to limit the flow of exciting current, the arrangement was such that the effect of load currents was to balance out the reactances of the coils. In later designs the reactance of the external coils was built into the preventive coils themselves so that the extra pieces of equipment are no longer required.

In 1937, during the search for a better scheme of control, it was suggested that a resistor of proper ohmic value be connected across the terminals of the preventive coil that was open-circuited during the transition. With this connection, while the preventive coil was operating "one-legged" the current would divide properly and flow through the resistor to the preventive coil and then to the motors. The value of resistance was determined by the maximum current to be encountered in the service of the locomotive and the designed voltage of the preventive coil. The voltage across the preventive coil at no time then would exceed its maximum designed value, and there would be no possibility of saturation of the coils.

Figure 2. Film 53—transition between notches 8 and 9 with transition resistor



Early Experiments With Transition Resistor

Such a scheme of connection was tried out upon the Pennsylvania Railroad in 1937, on locomotive 4819, at which time oscillograms were taken which showed very clearly the results to be expected from such an arrangement. In addition to the currents and voltages recorded on the films, it was also possible to measure directly the torque of the traction motors involved so that the results of the sag in tractive effort during ordinary notching and the lack of a sag in tractive effort in notching with the transition resistor were definitely recorded.

The drive from the traction motors to locomotive driving wheels is of the well-known spring cup type. In order to measure the motor torque, it was necessary only to provide an electric position-measuring device to indicate the deflection of the drive springs and record the indications on one of the oscillograph elements. When the spring constants are known, it is a simple matter thereafter to calibrate the indication of the element in pounds tractive effort.

The tests were confined to observing the effects on one transition only, namely from notch 8 to notch 9. The resistor used was of very small physical size, being capable of withstanding the currents involved for only a few cycles. The tests were all made at standstill, as it was impractical to measure the motor torque while the driving wheels of the locomotive were rotating.

The first of the oscillograph films (film 52) gives a record of the transition from notch 8 to notch 9 without the use of the resistor. Referring to film 52 (Figure 1) it will be observed that during the transition the tractive effort fell from 8,700 pounds per axle in notch 8 to 7,580 pounds per axle during the transition; the traction motor current fell from 3,530 peak amperes per motor to 2,880 peak amperes per motor and then rose again to 3,900 peak amperes per motor in notch 9 after

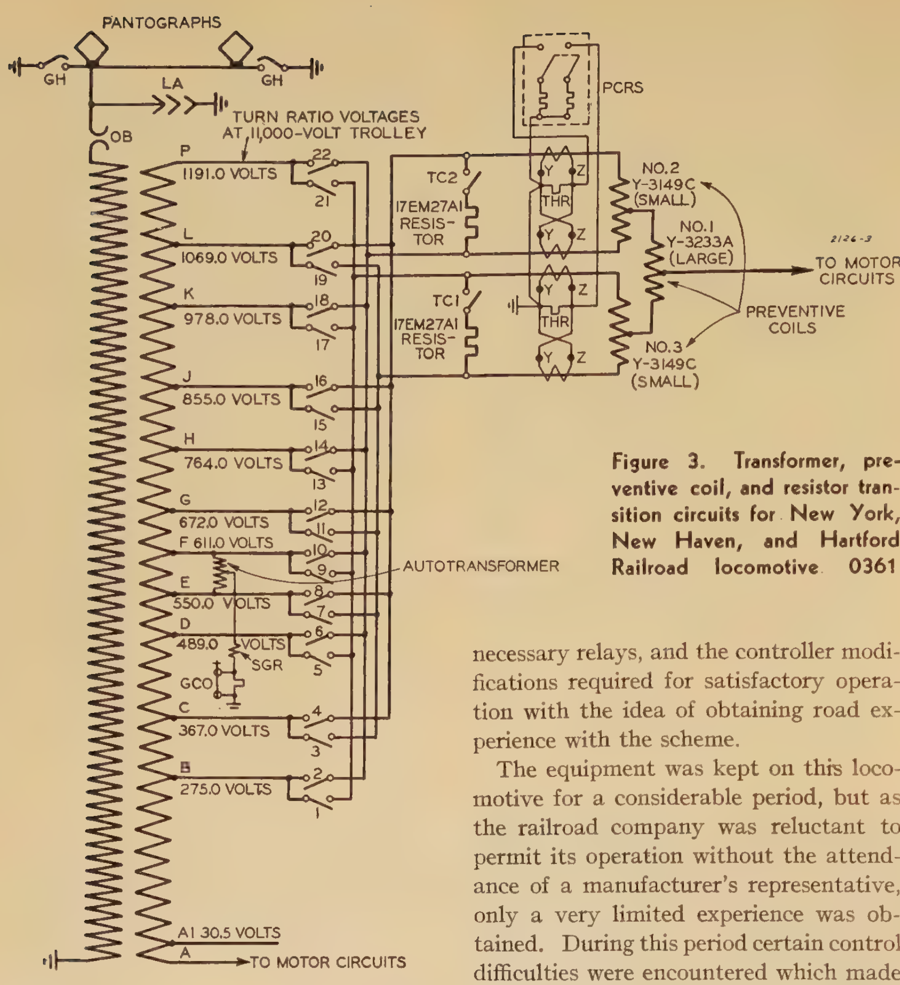


Figure 3. Transformer, preventive coil, and resistor transition circuits for New York, New Haven, and Hartford Railroad locomotive 0361

the transition was completed, this current corresponding to 8,910 pounds per axle as measured on the film. Also to be observed are the very high peaks in small preventive-coil voltage, which curve goes clear off the bottom edge of the film to a value impossible of determination. The rms value of the preventive-coil voltage for notch 8 is 43.5 volts, and for notch 9 is 49.8 volts.

Film 53 (Figure 2), taken immediately after film 52, with the transition resistor functioning, shows the very striking improvement obtained. In this case the tractive effort per axle moved gradually from 11,180 pounds per axle to 11,900 pounds per axle some time after the transition was complete. The increase in traction motor current was also gradual, raising from 3,680 peak amperes in notch 8 to 3,820 peak amperes during the transition, and then to 4,090 peak amperes after the transition was complete. The preventive-coil voltage is kept down to its normal value; the increase during the transition is only to 91 volts rms as compared with the nominal rating of the preventive coil of 128 volts.

Pennsylvania Railroad locomotive 4819 was subsequently equipped with a pair of resistors, a pair of resistor contactors, the necessary relays, and the controller modifications required for satisfactory operation with the idea of obtaining road experience with the scheme.

The equipment was kept on this locomotive for a considerable period, but as the railroad company was reluctant to permit its operation without the attendance of a manufacturer's representative, only a very limited experience was obtained. During this period certain control difficulties were encountered which made the scheme of getting the resistor switch closed before the tap switch opened, and then opening the resistor switch after the next higher tap switch had closed, somewhat unreliable. However, there was a noticeable improvement in locomotive acceleration and a very marked reduction in the amount of arcing on the tap switches, both in noise and the amount of visible arc, during the time the resistor transition equipment was working.

The equipment was subsequently removed from Pennsylvania Railroad locomotive 4819, and returned to the manufacturer's plant for reconditioning.

Service Experience on New York, New Haven, and Hartford Railroad

Having observed the performance of Pennsylvania Railroad locomotive 4819 with the resistor transition equipment functioning, New Haven representatives expressed interest and a desire to have the equipment turned over to them for use and service experience on one of their locomotives, in order to explore fully the possibilities of improved locomotive performance and reduced tap-switch maintenance.

New Haven electric locomotive 0361 (Figure 6) was selected for the installation of the resistor transition apparatus. This locomotive is one of six built by the General Electric Company in 1938 and used in main-line passenger service between New York and New Haven. A brief description of the locomotive follows:

Class	0361–0366
Service	Passenger
Power supply	11,000 volts, 25 cycles, a-c, 660 volts, d-c
Wheel arrangement	2C+C2
Number and type of traction motor	6GEA-622
Total weight	432,000 pounds
Weight on drivers	273,000 pounds
Length between coupler pulling faces	77 feet 0 inches
Over-all width	10 feet 2 inches
Height over a-c pantographs down	14 feet 8 inches
Maximum tractive effort (25% adh.)	68,500 pounds
Continuous tractive effort	24,100 pounds
Speed at continuous tractive effort	56.0 miles per hour
Continuous rated horsepower	3,600
Photograph	Figure 6

This locomotive has 22 tapping contactors and 20 a-c controller notches without intermediate buck-boost notches. No sequence relay interlocking is provided.

The application was made while the locomotive was in the shop for its first truck overhaul and traction-motor commutator turning after making a total mileage of 263,000. It was released from the shop July 16, 1941, after making various yard tests to determine that the new apparatus was functioning properly. Before assignment to any revenue trains, however, it was given a series of light runs on the main line, still further to insure that operation was correct.

In order to find out if duty on the tapping contactors was actually lessened by reason of the elimination of severe current and voltage transients in the preventive coils, new tips and arc chutes were installed on all such contactors. On subsequent periodic inspections burning and wear of tips and arc-chute sides could be compared with conditions on similar locomotives without the resistor transition.

The transformer and preventive coil connections for this locomotive are as shown in Figure 3. The control circuits in Figure 4 show the master controller and the relay circuits associated with the operation of the resistor transition switches $TC1$ and $TC2$. Figure 5 is a sequence diagram for the operation of the relays, tap switches, and resistor transition contactors for certain representative steps The following is a brief description of the operation of the control circuits which have as their function the closure of the resistor transition switch before the opening tap switch breaks its

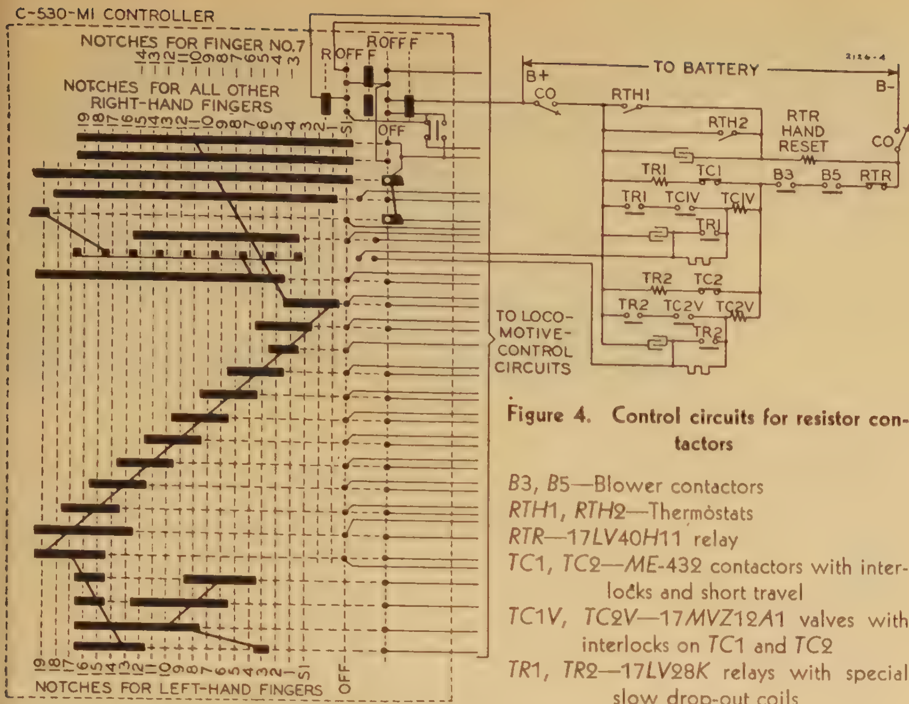


Figure 4. Control circuits for resistor contactors

B3, B5—Blower contactors
RTH1, RTH2—Thermostats
RTR—17*LV*40*H*11 relay
TC1, TC2—*ME*-432 contactors with interlocks and short travel
TC1V, TC2V—17*MVZ*12*A*1 valves with interlocks on *TC1* and *TC2*
TR1, TR2—17*LV*28*K* relays with special slow drop-out coils

contacts, and thereafter the holding of the resistor transition switch closed until the next tap switch is closed.

Upon the movement of the master controller from one notch to the next, a segment between notches gives an impulse of electric energy to an appropriate wire which is connected to the magnet valve of the resistor transition switch it is desired to close. The operating coil of the magnet valve is wound for approximately half the normal control voltage of the system so that its operation will be very fast. The coil is protected against the control voltage, should the controller be left midway between notches, by a resistor that is shorted out of the circuit by a relay (*TR*1 or *TR*2), whose circuits are made up before the transition is initiated, as described later. Should the controller remain between notches, the relay is de-energized by interlocks on the resistor switches, thereby inserting the protective resistance in series with the magnet valve coil. Attached to the armature of the magnet valve is an auxiliary normally open contact (*TC*1*V*, *TC*2*V*) which "makes" upon the initial travel of the armature before the latter starts to open the air valve. This contact forms a holding circuit from the battery to cause the magnet valve to continue to close, thus operating the air valve and supplying air to the cylinder of the contactor, regardless of the length of time the impulse remains on the wire from the controller. The transition switch closing its main contacts also opens the circuit to the transition relay by means of interlocks. Another set of transition relay contacts (*TR*1, *TR*2) are in series with the holding circuit to the magnet valve of the particular switch involved. The transition relay has a time delay built into it of sufficient duration to keep its contacts closed until enough time has elapsed for the transition of the main tap switches to have been completed. At the end of the prescribed time the transition relay operates, opening the holding circuit and causing the transition contactor to open. The interlocks on the transition contactor (*TC*1, *TC*2) again make up the circuit to the transition relay, which again "picks up" and is then ready for a repetition of the same cycle when it is initiated by an impulse from the master controller. There are two sets of relay equipments, one for each transition resistor.

The resistors are located over the exhaust air stream from the radiator of the main transformer, and the control is so interlocked with the blowers that it will not operate unless the blowers are running. As another safety factor a thermostat has been placed over each resistor so that, when they exceed a safe temperature, the thermostat will close, energizing the thermostat relay (*RTR*) which latches in its energized position and de-energizes the resistor transition circuits. The resistor transition equipment cannot then be operated until the resistor has cooled, and the thermostat relay has been reset by hand. The resistor thermostats are in the nature of "backup" protection, as the resistors themselves have been designed liberally enough so that, with the aid of the ventilation from the transformer radiator, it would be permissible to let them stay on the preventive-coil bus energized continuously without damage to themselves. They have a very high temperature coefficient of resistance so that, as they heat up, they inherently reduce the current flowing through them. However, in order to protect the adjacent equipment and cab structure from excessive heat, it was considered desirable to add the thermostat to each resistor.

The resistor transition switches are standard tap switches equipped with a reduced tip gap to obtain short travel and space filler to reduce the surplus volume in the cylinder. Minimizing the clearance volume of the cylinder reduces the volume of air required to start the switch moving, and reducing the switch travel cuts down the time required for closure. These refinements in the switch, combined with a magnet valve that is operated on approximately twice normal voltage, suffice to give a switch with the required operating speed.

This arrangement of the control circuits appears to have been eminently satisfactory, and where the segments in the master controller have been properly aligned so that the impulse to the magnet valve is started before its corresponding tap switch has been de-energized, no difficulty has arisen in insuring that the resistor switch is closed before the tap switch breaks its contacts. In normal notching the duration of the impulse from the master controller is of the order of $^1/_{100}$ of a second.

When the trial of resistor transition on a New Haven electric locomotive was first considered, two trains immediately came to mind as offering the best chance to observe the effects. These two trains were exceptionally heavy morning and night

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A-C STEP	CONTACTORS															
	1	2	3	4	5	6	7	8	9	10	11	12	TC1	TC2	TR1	TR2
S1	●		●												●	●
1	●	●	●	●											●	●
TRANSITION	●	●	●	●									●		●	●
		●	●	●									●		●	●
		●	●	●	●								●		●	●
		●	●	●	●								●			●
2		●	●	●	●										●	●
TRANSITION		●	●	●	●									●	●	●
			●	●	●									●	●	●
			●	●	●	●								●	●	●
			●	●	●	●								●	●	
3			●	●	●	●									●	●
TRANSITION			●	●	●	●							●		●	●
				●	●	●							●		●	●
				●	●	●	●						●		●	●
				●	●	●	●						●			●
4				●	●	●	●								●	●
TRANSITION				●	●	●	●							●	●	●
					●	●	●							●	●	●
					●	●	●	●						●	●	●
					●	●	●	●						●	●	
5					●	●	●	●							●	●
TRANSITION					●	●	●	●					●		●	●
						●	●	●					●		●	●
						●	●	●	●				●		●	●
						●	●	●	●				●			●
6						●	●	●	●						●	●

Figure 5. Sequence diagram for representative notch transitions with transition resistors

commuter trains operating as local expresses between New York and New Haven; each required higher locomotive output than any other passenger train handled electrically. The electric locomotives regularly assigned were the 0361–0366 class. Short-time load demands, when operating the two trains in question, reach a peak of 7,600 horsepower, more than twice the continuous rating. These peak demands come about through frequent stops on a fast schedule for half of the run in each direction.

These two trains therefore were selected for the initial test runs. Other trains were handled as the locomotive was used in the regular 0361–0366 pool.

Two results of the resistor transition were immediately apparent. One was the virtual elimination of the usual barking of contactors on heavy current during acceleration. The second was the smoothing out of the tractive effort increments on acceleration, resulting in less tendency for the locomotive drivers to slip. This was remarked upon almost immediately by those operating the locomotive.

At first, the resistor transition apparatus was cut out except when a road foreman or others familiar with the equipment were riding the locomotive. In a short time, however, it became apparent that it could satisfactorily be left cut in, depending in the event of trouble on the thermostats or the instructions issued to the crews for cutting it out. The latter was a simple process, involving only the opening of a small double pole, single-throw control knife switch which made the transition contactors inoperative and returned the locomotive to normal operation.

Experience in regular operation since this time has developed practically no trouble, and the equipment has been continually in service and cut in, except for several instances of thermal relays tripping, probably due to enginemen operating in between notches on the master controller, and one period of approximately two weeks due to an error in judging the position of the timing relay contacts. The eight months of use have demonstrated that the resistor transition equipment is reliable in its operation without any more attention than is given other electric apparatus on the locomotive.

The duty on tapping contactors has been lessened. Unfortunately, due to operating conditions, there was no opportunity to determine quantitatively how much benefit was obtained, but qualitatively much benefit was observable. This means that aside from the operating advantages already described, there was a reduction in maintenance of tapping contactors, which is an important item on a-c electric locomotives. Against this, however, must be placed the additional maintenance of the resistor transition equipment, particularly the contactors and the segments and fingers on the master controllers.

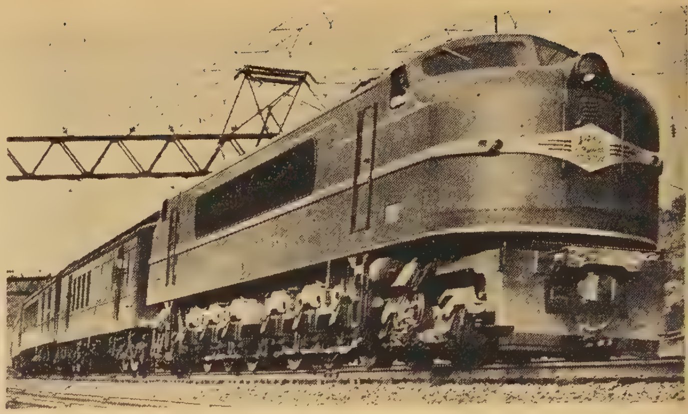

Figure 6. New York, New Haven, and Hartford Railroad Company locomotive 0361 upon which resistor transition equipment is operating

Another feature of the equipment is that a certain amount of care must be taken by the engineman when operating the controller. The control-circuit scheme is based on a train of events initiated by an impulse from the controller, and subsequent impulses must be spaced far enough apart in time so that the initial train of events has been completed, or else the most desirable operation is not obtained. When accelerating from standstill, a short pause normally occurs between each two notches, and the operation is always correct, but in notching on or off too fast when up to speed, the resistor transition may be only partly effective because of lack of time between notches. This is only of interest from the standpoint of duty on preventive coils and tapping contactors; tractive effort considerations are not critical under these conditions.

Conclusions

While the resistor transition equipment was applied to a New Haven passenger locomotive as representing one of the latest type of a-c locomotives in service, it would seem that the application would find its greatest usefulness in freight locomotives where smooth-starting tractive-effort characteristics are practically a necessity, and where starting duty is both prolonged and severe.

In general, the resistor transition scheme, developed and applied as described, has given gratifying results in its apparent improvement in the acceleration of the locomotives to which it has been applied, through the eduction in the arcing duty on the tap switches as evidenced by the almost complete lack of any arc noise and the very much smoother operation of the locomotive when notching.

Although the resistor transition has a beneficial effect on tap-switch surges after closure of the higher tap switch during a transition, because of the fact that it will prevent saturation, particularly of the large preventive coil, it is still necessary to analyze carefully each case to insure that excessive tap-switch surges are not obtained with the given values of series air-core reactance in the small coils. The fact that the transition resistor is still energized after the next higher tap switch has closed means that the current flowing through the resistor is superimposed upon the small preventive-coil exciting current, with the result that the surge current may actually not be greatly different from what it would have been, had a resistor not been connected to the circuit. However, with some combinations of equipment, the presence of the resistor will result in a definite reduction in the surge, and each case must therefore be analyzed on the basis of its own merits.

A further study of the scheme and its application would seem to be warranted for heavy duty a-c freight and passenger locomotives.

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Electric Equipment for Large Electrochemical Installations

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Synopsis: The increased demand for aluminum, magnesium, chlorine, copper, and zinc for war purposes has made the electrolytic processes for these materials the largest consumers of electric energy in this country.

The most frequently encountered d-c and voltage requirements of these four principal electrolytic processes are discussed. An illustrative current-time and voltage-time characteristic curve for starting a chlorine cell line is shown, and the requirements that such characteristics impose on the electric equipment are discussed. The current-voltage characteristics of aluminum, magnesium, chlorine, and zinc cell lines are also shown.

A typical installation of conversion equipment for an electrolytic process plant is given, and the reasons for selecting the particular types of electric equipment and its physical and electrical arrangement are discussed.

As part of this discussion, there are included characteristic curves of rectifiers showing the effect of ignition control on power factor for 6-, 12-, and 36-phase combinations. A table giving a "rule-of-thumb" relationship between the number of phases and kilowatt limits which have been found in practice to provide operation reasonably free from telephone interference is included. There is also given a tabulation of phase shifter combinations, by means of which multiphase operation can be obtained with various combinations of standard six-phase rectifier transformers.

THE electrochemical industry has very recently become one of the most important consumers of electric energy in the United States with increasing production of aluminum and magnesium and, in the near future, will become an even more important user. The "Big Four" of the electrochemical industry—aluminum, magnesium, chlorine, and the sister metals, copper and zinc—have now installed over 2,500,000 kw of conversion apparatus of all types in this country, and the end of the year 1942 will see an additional million kilowatts installed in these four branches of the electrochemical industry.

This conversion equipment operates at almost 100 per cent load factor and converts power at a yearly rate of 25,000,000,000 kilowatt-hours, or nearly one quarter of the total energy used for all industrial purposes. This percentage may at the end of this year well increase to perhaps 40 per cent of the total industrial power.

There are many other electrochemical processes of less importance. Perhaps the next largest in installed conversion capacity produces fused sodium chloride with approximately 60,000 kw. Following this in importance are processes for production of hydrogen, manganese, potassium perchlorate and sodium chlorate, and other similar processes. The application problems are similar to those of the four major industries.

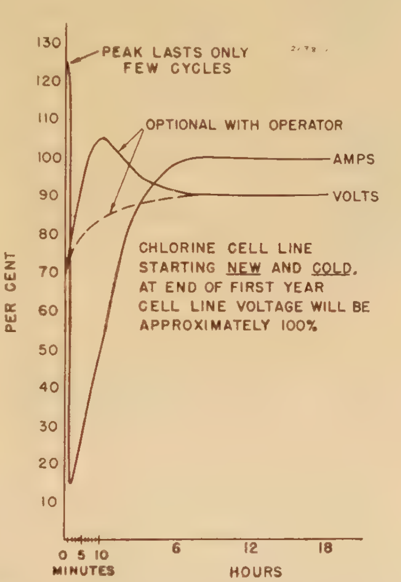

Figure 1. Chlorine cell-line characteristic

While these figures include conversion apparatus of all types (rectifiers, rotary converters, and motor generator sets), the rectifier is now the prevalent choice of the industry for converting such large amounts of power into direct current. Since 1937 very few large electrolytic installations using rotating conversion equipment were made in this country or in central Europe. During the last two or three years, the ignitron-type rectifier has been chosen for most of the installations in this country, although the multianode type of rectifier is still the choice of foreign designers.

The application engineer dealing with the electrochemical industry is at a certain disadvantage compared with application engineers for other industries, inasmuch as the chemical industry has published very little data concerning their electrical requirements. This may be attributed to the fact that electrochemical processes and techniques are often of a highly secret nature, not only in time of war, but also under peacetime conditions when the process is a matter of competitive endeavor. The electrical-application engineers would welcome more complete and precise knowledge of the electrical characteristics of the various electrolytic cells, presented not from the point of view of the chemist, but from the point of view of the electrical engineer. A few typical characteristics are discussed below, but this subject needs considerably more analysis.

Electrochemical Process Characteristics

Aluminum

A typical electrolytic cell line or "pot" line for the production of aluminum consists of a number of cells connected in series and is supplied with a normal direct current of 60,000 amperes at perhaps 650 volts. Such a line may require very little adjustment in d-c voltage except for minor variations of cell resistance and of the counter electromotive force of the cells. Adjustable voltage is however required, if the number of cells is decreased below normal, or for baking out a new or "green" cell line. The variation of the a-c supply voltage may require means of maintaining pot-line current and voltage at a constant value. Means for accomplishing such voltage regulation will be discussed later. Under ordinary operating conditions, after such a line is once put into operation, it will run for months or even years, with a voltage and current variation of not more than five or ten per cent.

Another typical aluminum cell line with cells of the so-called Soderberg type has a normal capacity of 32,000 amperes at 650 volts. With this type of cell more frequent adjustment in the d-c voltage is found very useful for the purpose of starting "green" cells or baking out cell linings.

Magnesium

As far as the application of the electrical conversion equipment is concerned, a magnesium cell-line is very similar to the

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aluminum line. The magnesium cell line may have a normal d-c rating of 60,000 amperes at 600 volts, although a large installation is now in the process of erection which, based on European practice, will use a large number of 20,000-ampere 350-volt cell lines. Another installation is using 32,000-ampere 600-volt lines.

Wider adjustment in the d-c voltage is required on a magnesium cell line as compared with one for aluminum, because a new cell line is usually started with only a few cells and is built up over a period of several weeks by adding new cells in series. Both in this country and abroad, installations have been made with a wide range of adjustable d-c voltage, even though normal operation is at practically constant voltage.

Chlorine

Typical chlorine cell lines use from 7,500 to 10,000 amperes at 500 to 700 volts, depending upon the type of cell used or the number of cells connected in series. Some cells are designed for 1,800 to 2,000 amperes, and four or five of these may be connected in parallel on a common d-c bus. Usually each 7,500- or 10,000-ampere cell line is supplied from one rectifier unit, without interconnection with other cell lines on the d-c side except in case of emergency. This arrangement provides flexibility to meet the day-to-day variations in cell-line requirements. Such lines are operated at essentially constant d-c voltage. Figure 1 shows typical voltage- and current-time curves for a chlorine line which is placed into operation for the first time.

It should be pointed out that these curves are not based on tests, as the authors might desire, but on previously published information and on theoretical considerations.

The Figure 1 shows an initial instantaneous peak rising to perhaps 125 per cent after the cell line is first thrown on full voltage and before the counter electromotive force or battery action of the cell is established, a phenomenon which happens within a few cycles. The sodium-chloride solution initially has a high resistance, while it is cold and dilute. For the next six or eight hours the current rises as the resistance decreases with increasing concentration of the solution.

The initial voltage reduction to 60 or 70 per cent required of the conversion equipment to keep down the initial inrush of current is well within the capability of ignition control of a rectifier. Minor adjustments in the voltage required from day to day are also usually made by means of ignition control. Larger d-c voltage changes may be obtained with taps on the rectifier transformer as will be discussed later.

It should be noted that the characteristics of the individual cells are somewhat obscured if a number of lines are operated in parallel.

Copper and Zinc

A typical copper or zinc cell line is quite different from the chlorine line as regards the conversion equipment. A zinc line may require about 10,000 amperes at 500 to 600 volts, depending on the number of cells connected in series. Foreign installations have been made using voltages up to 800 or 850 volts. For normal operation the cell line is operated at substantially constant d-c voltage. Usual applications require some transformer taps for d-c voltage adjustment on a cell line as the number of cells in series are changed.

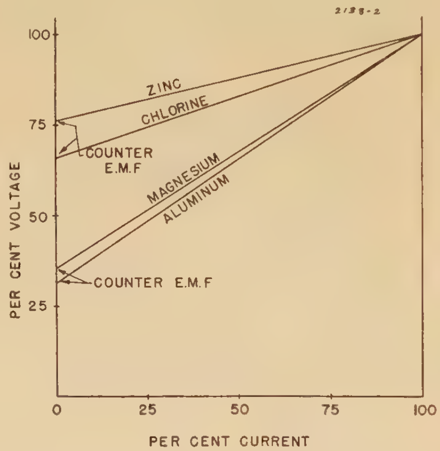


Figure 2. Electrolytic cell-line characteristic

Since, however, the electrolytic production of copper or zinc is a batch process, where a cell is periodically taken out of service for the purpose of stripping copper or zinc from the electrodes, the cell-line voltage must at the same time be lowered to such a low value that the disconnection of one cell may be made at slightly above zero current. The voltage reduction required to accomplish this is approximately 25 per cent, usually obtained by ignition retard.

Figure 2 shows a typical voltage-current characteristic of a zinc line. It is easily understood that the removal of one cell from the circuit by means of short-circuiting this cell should, from the standpoint of operation, be done as quickly as possible in order to revert to full current and full production without too much loss of time.

It is important that some cell current be maintained, at least at a low value, at all times to prevent the copper or zinc already deposited from going back into solution.

Figure 2 also shows cell-line characteristics for other cell lines. Chlorine has a counter electromotive force of approximately 66 per cent. For alumina dissolved in cryolite, the authors estimate a counter electromotive force of about 30 per cent based on the theoretical voltage of dissociation of 1.38 volts per cell and the approximate total voltage drop of 5.9 volts per cell. The counter electromotive force of fused magnesium chloride based on similar data is shown to be approximately 35 per cent. The authors have no accurate data on the length of time for which this counter electromotive force persists after an interruption in the current supply.

Some cell lines have their mid-point grounded through a resistor for the purpose of lowering the cell potential to ground. Such installations usually have a relay and alarm system in the ground connection to notify the operator of accidental grounds on the cell circuit. Grounding the mid-point of the cell line seems to be common practice abroad but is only occasionally used in this country. The disadvantages of its use are slightly more electrical losses to ground because of current bypassing some cells.

Selection of Electric Equipment for Electrochemical Processes

Rectifiers Versus Rotating Apparatus

The mercury-arc rectifier has become so well established in the electrochemical field that it is hardly necessary to dwell at great lengths on the advantages it has over rotating apparatus. However, it is desirable to review very briefly some of these advantages.

The space requirements for the installation of rectifiers are quite moderate, and no reinforced foundations are needed such as are required for rotating apparatus.

In the larger units at 600 volts direct current, the rectifier-conversion equipment may be installed for approximately $35 to $40 per kw. This includes the incoming 13.8 kv 60-cycle a-c switchgear, autotransformers, rectifier apparatus, cable and copper bus bars for the main d-c bus, building, and installation labor. This approximate installation cost figure would cover a layout as shown in Figures 5A and 5B.

An especially important consideration in wartime is the fact that the amount of particularly scarce materials, such as copper and steel, is considerably less in a

rectifier installation than in rotating apparatus. Incidentally, it is for this reason that the rectifiers have for some time been more widely used in Europe than they have been in this country.

The efficiency of the modern ignitron-rectifier equipment in the voltage range around 600 volts is in excess of 95 per cent, including the transformer losses and the power consumed by the rectifier auxiliary equipment, such as pumps and ignition apparatus.

The efficiency of the conversion equipment is of special importance in the electrochemical industry, because these processes operate at close to unity load factor, and the electric energy which the conversion equipment supplies over a year's time may cost as much or more than the cost of the conversion equipment itself.

An operating advantage of the rectifier installation over a rotating-apparatus installation is particularly emphasized by the problem of restarting a large-capacity cell line after a complete power failure or shutdown. Assuming that such a cell line is supplied from six 10,000-ampere rotary converters, it is necessary after a power failure to restart and resynchronize all of the rotary converters with the d-c breakers open. Since it is impossible to close all of the d-c breakers simultaneously, one of the rotary converters will assume the entire load, its breaker will trip off on overload, and succeeding breakers will follow.

Table I. Phase Shifters Required for Multiphase Operation

Rectifier Transformers: Number of 6-Phase Units	Rectifier Transformers: High-Voltage Connections	Phase-Shifting Auto-transformers Required	Number of Phases Operation
1	Delta or wye	None	6
2	1 delta, 1 wye	None	12
3	2 delta, 1 wye	2 10-degree	18
4	2 delta, 2 wye	2 15-degree	24
5	3 delta	2 12-degree	30
	2 wye	2 6-degree	
6	3 delta, 3 wye	4 10-degree	36
7	4 delta, 3 wye	2 4.29-degree; 2 8.57-degree; 2 12.86-degree	42
8	4 delta	2 15-degree	48
	4 wye	4 7 1/2-degree	

To meet this problem with rotary converters, a large cell-line breaker is resorted to, which may be rated 60,000 amperes continuously. By using this rather unwieldy device, all of the rotary converters may be paralleled on the d-c bus with this cell-line breaker open and then the breaker closed.

A rectifier application easily circumvents this problem. With rectifiers, all d-c and a-c breakers are closed with the ignition circuit de-energized. The potroom load is picked up by energizing the ignition circuits of all rectifiers simultaneously. No cell-line breaker is needed. It is obvious that the time element, in reestablishing service after a power interruption, is very small indeed for a rectifier station as compared with a rotary converter installation.

Rectifiers provide a certain amount of d-c voltage adjustment by means of ignition retard, which allows quick and easy compensation for changes in the a-c supply voltage or for changes in the cell-line conditions; whereas with rotary converters, the use of boosters must be resorted to. It should be pointed out, however, that the continuous use of ignition control is not resorted to in practice beyond four or five per cent, since a wider use is accompanied by lowering of the power factor and an increase in the tendency of the rectifier to arc back.

Figure 3. Large rectifier substation

All in all, it may be fairly stated that the many advantages of rectifiers over rotating apparatus makes their selection for a large electrochemical installation a foregone conclusion.

A considerable advance in the art of rectifier design is represented by the single-anode rectifier tank. As illustrated in Figure 3, these single-anode tanks or ignitrons are assembled in groups of 12 to supply 5,000 cathode amperes. This ignitron design has resulted in an increase of efficiency by a reduction of the arc drop to 20 volts or less; which, for such installations, means an over-all efficiency of about 95 per cent. The lower arc drop and the high efficiency result from the fact that the anode encased in a single tank can be placed closer to the mercury-pool cathode.

Because each anode is confined in a relatively small tank compared to the large multianode arrangement, with the ignitron it is possible to make much tighter vacuum seals, and therefore better to maintain the vacuum. Furthermore, if a tank leaks, or trouble develops with one tank, it is a relatively simple matter to replace this tank, whereas it is much more serious when a large multianode tank has to be taken out of service. For these reasons the single-anode ignitron rectifier is now the choice of the electrochemical industry, foreign countries excepted.

Selection of Rectifiers

Figure 4 shows the general arrangement of an electric system for three 60,000-ampere 600-volt electrolytic cell lines. While this sketch does not represent in detail an actual installation, nevertheless all of the features shown are commonly installed, and the sketch will serve as a basis for discussion of the various reasons why the equipment shown was selected.

Each 60,000-ampere cell line operates as a unit. Therefore, the largest available size rectifier units are chosen, in order to obtain maximum economy in first cost. The rectifier transformers are selected so as to supply two rectifier units from one transformer, or a total of 10,000 d-c cathode amperes. This is about the upper limit. The maximum of 10,000 cathode amperes d-c per rectifier transformer has been found both in this coun-

try and abroad to be a conservative upper limit because of large copper sizes to be handled, stresses in the transformer during arc-back, and for other design reasons. Thus the logical choice of rectifier units for this case is the 5,000-ampere, 12-anode ignitron, similar to those shown in Figure 3.

In view of its relative simplicity of design and sturdiness of construction, the six-phase double-wye or quadruple-wye secondary connection is most widely used, both here and in foreign countries. Some 12-phase rectifier transformers have, of course, been used for small installations. As shown on Figure 4, the transformer selected has quadruple-wye secondary connections. Two wye, with their interphase winding, supply one 5,000-ampere rectifier, and two duplicate wye, with interphase winding, supply the other rectifier.

Number of Phases

As the six-phase, 10,000-ampere cathode d-c rectifier transformer is selected, there are now required six such units to supply the 60,000-ampere pot line. This is a total of 36,000 d-c kw. Investigations of rectifier wave shape and its influence on telephone communications indicate that the telephone influence is, to a very large extent, dependent on the characteristics and the total installed capacity of the a-c supply system, to which the rectifier installation is connected, and also on the number of phases selected. It has been fairly well established that such an amount of power operating at six phase would cause widespread telephone interference, if no co-ordinative measures are taken. Therefore, the number of phases should be selected with reference to the ratio of the total rectifier capacity and the total system capacity supplying the rectifier installation.

Table II contains a "rule of thumb" of rectifier kilowatt ratings and number of phases selected for 60-cycle rectifier operation. This table is, of course, subject to interpretation and should be used with extreme caution, since, for instance, there have been some cases of telephone interference caused by 200-kw rectifiers at the end of a long feeder. It is, however, generally applicable to rectifier installations on relatively large power systems; and so far as the authors know, there have been no cases of telephone interference caused by rectifiers in industrial plants within the limits shown in Table II.

The total kilowatts and the number of rectifier transformers on each pot line indicate in accordance with Tables I and II that the installation here discussed should use 36-phase relationship. Since there are six six-phase units, the rectifier tubes will fire in 36-phase relationship, if the primary voltages are shifted by ten degrees for each rectifier unit.

Table I gives a convenient reference for the number of phase shifters and the degree of phase shift required for combinations of six-phase transformers for multiphase operation. The use of 18-phase operation is inadvisable, because its principal harmonics are the 17th and 19th, having the greatest audio-sensitivity. The 36-phase relationship eliminates theoretically all harmonics below the 35th.

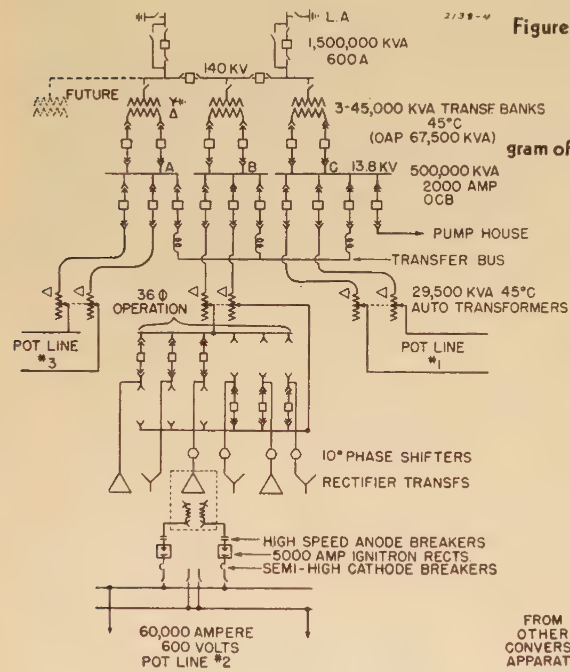
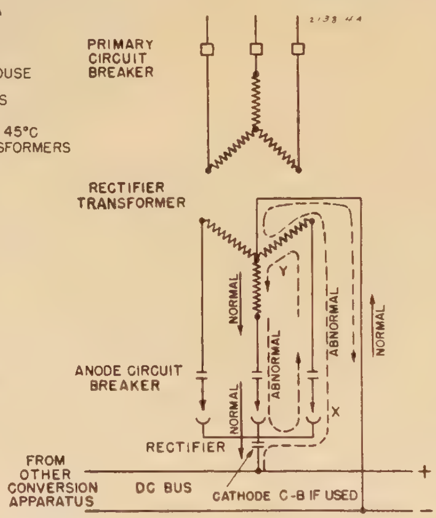


Figure 4 (left). Electric system for large electrochemical installation

Figure 4A (below). Simplified diagram of rectifier with arc back (*X*) on one anode



It has generally been found impractical to make sufficiently accurate estimates of the telephone influence in advance of a rectifier installation, because of the many variables and unknowns, which allow only approximate estimates of the telephone influence. Therefore, it has become the practice, supported by experience, to evaluate only approximately the telephone influence and choose the number of phases in some such "rule-of-thumb" fashion as shown in Table II.

If it should ever become necessary in a specific case, remedies such as increasing the number of phases or a-c line filters can be applied after the installation has been made.

It should be clearly borne in mind that the foregoing discussion refers to a 60-cycle supply. Since telephone influence is sensitive to certain harmonics, Table II does not apply to 25-cycle systems. An installation with the same number of kilowatts should have a little over double the number of phases for a 25-cycle system as it has for a 60-cycle system.

In addition to the reduction in telephone influence, the multiphase operation offers definite economic advantages, since the increase in the number of phases improves the power factor as indicated in Figure 6. This figure is based on design data of an actual installation whose basic unit is the six-phase transformer. It can be seen that when the number of phases is raised from 6 to 36, the power factor improves from 92 to 95 at zero ignition retard. The increase in the number of phases also reduces the losses.

Instead of using basic six-phase transformer units, it is possible to use basic 12-phase transformer units. Such 12-phase transformers are much more commonly used in Europe than in this country, although they may be considered practical devices and have a definite field of application for smaller installations. All interphase transformers, indicated in Figure 4, are usually built in the common transformer tank.

The introduction of the phase-shifting autotransformer, as shown in Figure 4, results in some additional reactance in the circuit which would cause it to take slightly less load than a circuit without phase shifter. However, such unequal load distribution is easily compensated by a slight amount of ignition retard on the unit without phase shifter, and it is not considered necessary to insert any external reactance, although this is the practice in foreign countries.

The rating of rectifier transformers for

chemical installations is on a basis of 45 degrees centigrade temperature rise for continuous full load. This conservative rating for the transformers has universal acceptance in this country for electrochemical service, because the load factor is almost 100 per cent, and such installations may run for years under this full load condition. The 45 degrees centigrade temperature rise greatly reduces the maintenance on the oil and also provides some inherent spare capacity for emergencies, when it may be necessary to run the cell line on a reduced number of rectifiers, while one unit is undergoing maintenance.

With conservative design margins in both transformers and rectifiers, and with the high degree of reliability of modern rectifiers, such an installation as shown in Figure 4 would not normally require additional spare capacity. If the cell line is run with five instead of six units the overload is 20 per cent. It is generally practical, although not always desirable, to reduce the cell-line load while the single unit is off the line for maintenance. If it is essential to run the cell line with 100 per cent current at all times, a 10,000-ampere spare unit should be provided.

D-C Voltage Control

The installation illustrated by Figure 4 is assumed to require operation at d-c voltages as low as 100 for starting up a new cell line with only a few cells. Furthermore, the a-c voltage supply is assumed to vary five or six per cent from day to day, a condition which would cause the cell-line current to vary a slightly greater percentage. Both these conditions are frequently met in actual operation.

It is quite inadvisable to reduce the voltage from 600 down to 100 volts by ignition control. Figure 6 indicates that voltage reduction by ignition control, even as little as 16 per cent, reduces the power factor from 95 per cent to 82 per cent. Furthermore, operation of mercury-arc rectifiers with a large amount of ignition retard, for a long period of time, will increase the frequency of arc-backs. Experienced rectifier operators, therefore, use as little ignition retard as possible, and the application of ignition control should be limited to such cases where the voltage reduction is of only temporary character, as, for instance, when starting a cell line, where ignition retard of 30 to 40 per cent is permissible for a few minutes.

The other method of d-c voltage control, which uses a change of rectifier-transformer voltage, is not connected with these drawbacks. If only five or ten per cent change of d-c voltage is required over a long period of time, as would be the case for a chlorine line, this change should be obtained by taps in the rectifier-transformer high-voltage winding. These taps would be changed with the transformer de-energized, as such changes are sufficiently infrequent to make an interruption for this purpose acceptable.

However, with the change of d-c voltage required in the case of the installation here discussed, the only practical way of accomplishing this change of a-c voltage is through the use of an autotransformer with no-load taps. In this particular case the autotransformer would be supplied with seven no-load taps from 100 to 600 d-c volts rectifier output. Since these rather large changes in d-c voltage are required only once in several weeks, as a number of cells are added or removed from the circuit, changing these taps with the cell line de-energized imposes no serious hardship on the cell-room operator.

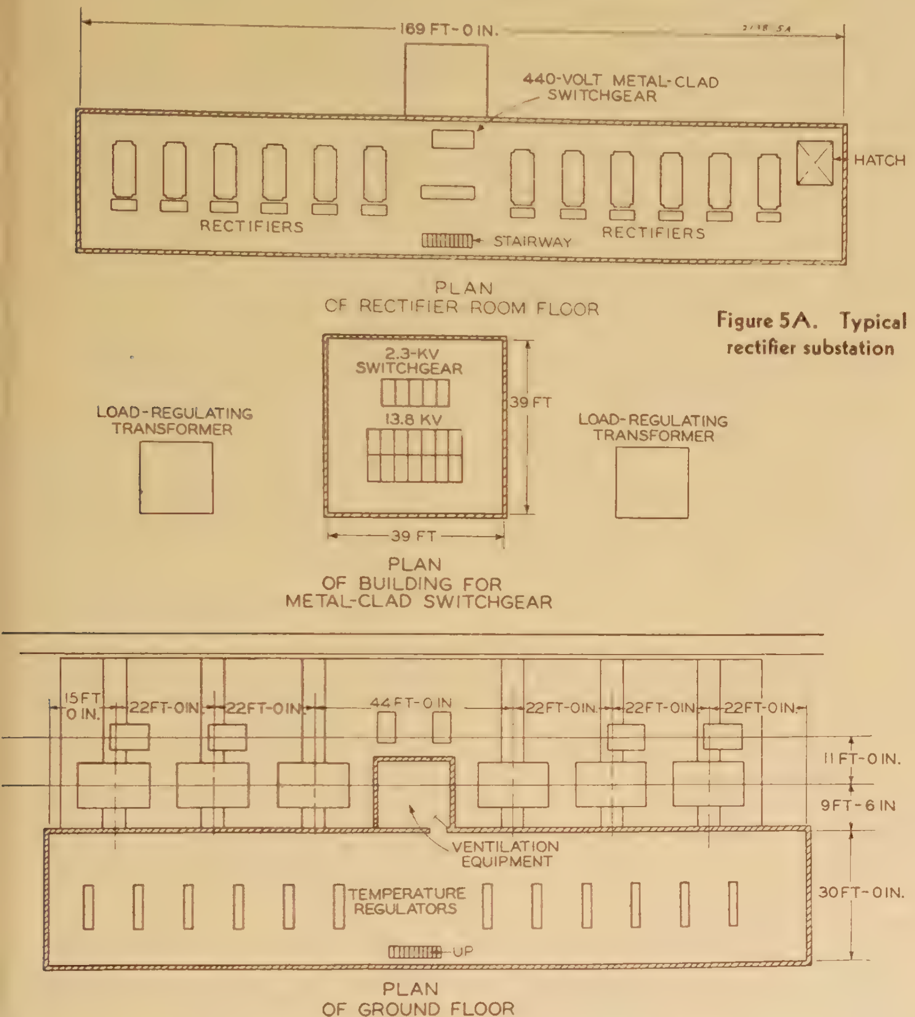


Figure 5A. Typical rectifier substation

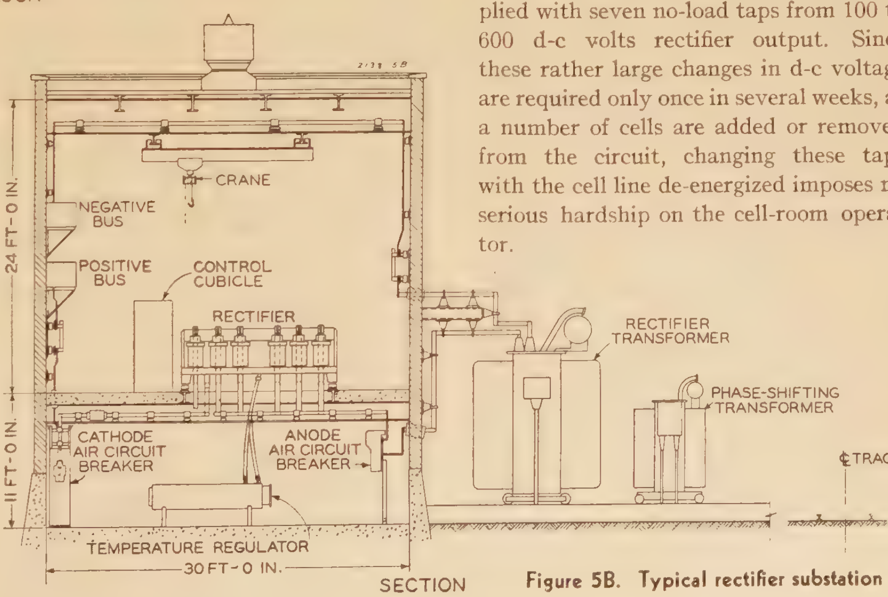


Figure 5B. Typical rectifier substation

In addition to the no-load taps, the autotransformer should in this case have load-ratio control to provide a plus or minus 60 d-c volts adjustment within the range of any of the seven taps of the no-load tap changer. This results in extremely flexible control and permits the operator to compensate for fluctuations in the incoming a-c power supply and also to bridge the gap between the no-load taps. The total range of 120 d-c volts load ratio control has been covered with 16 and 32 steps. Load-ratio control is also quite useful to the operator in maintaining constant cell-line current, as cell conditions change, and operators, who have this tool available, attest to its usefulness for this purpose.

The autotransformer with these features is also a tool for reducing the d-c voltage for the purpose of baking out a "green" cell line or for short-circuiting the d-c bus and simultaneously baking out all rectifier units themselves when they are first installed.

An exactly similar result can be obtained by the use of an induction voltage regulator when due attention is paid to the shifting of the primary voltage supply with respect to the ignition-system voltage. Induction voltage regulators are somewhat more expensive but are frequently used abroad.

With such flexible tools for voltage control available, it is not necessary to use any considerable amount of ignition control, except to balance the load between the various rectifier units.

The load-ratio control feature of the autotransformer makes possible automatic kilowatt input control for the pot line. This feature is not required by the cell-room operator, who will generally take all the kilowatts he can obtain, but by consideration of power contract formulas. By using constant kilowatt control, the load factor can be maintained at better than 98 per cent, and, with some power contract formulas, this feature alone will pay for the autotransformer very quickly.

It will be noted from Figure 4 that two autotransformers are chosen for each pot line. Each autotransformer is of sufficient size to supply with normal temperature rise four rectifier transformers and, in an emergency, five rectifier transformers. This spare capacity is required for reliability. When one autotransformer is out of service for maintenance of load-ratio control contacts, or in the event of failure of a cable connection to the main bus, the cell line can be operated for the duration of the emergency at five-sixths capacity.

Autotransformers, which are usually wye-connected, often have a tertiary stabilizing delta winding, as indicated in Figure 4. This tertiary winding will be of a certain minimum size as dictated by design consideration. If designed for a standard voltage commonly used to supply auxiliary plant load, the tertiary winding can be made to serve a double purpose. In a particular case, each tertiary winding was designed for 3,000 kva at 2,400 volts. This provides a very economical source of plant power, since no additional 13.8-kv high-voltage switching is required to make it available, and very little additional copper in the tertiary winding.

The autotransformer winding, being 13.8-kv wye-connected, provides a convenient means of grounding the neutral of the 13.8-kv system. While not shown by Figure 4, in an actual installation all six of the autotransformer neutrals are brought through disconnecting switches to a common grounding bus; and this bus is connected to ground through a current limiting resistor. Such neutral grounding greatly facilitates the relaying of the system and adds to its reliability and safety.

Table II. "Rule-of-Thumb" Kilowatt Ratings and Phases for 60-Cycle Rectifier Operation on Relatively Large Power Systems

Kilowatts	Phases
1,000– 2,000	6
2,000– 6,000	12
6,000–20,000	24
20,000–30,000	30
30,000–50,000	36
50,000 and up	48–72

Automatic Current Regulation

Automatic current regulation can be furnished by means of regulators operating on the rectifier ignition circuits. In general, such devices are not required for electrochemical installations, because the conditions are sufficiently constant, and they have been applied only to meet special conditions. They may be justified when the a-c system voltage varies, and it is desired to keep the d-c pot-line current constant. They are also of value where the rectifier must parallel with other conversion units of dissimilar characteristics.

Where such devices are used, the amount of ignition control is limited, and the undesirable effects on power factor are avoided. Where they are used in conjunction with load-ratio-control autotransformers, the ignition control is limited to perhaps five or six per cent range, and when these limits are reached, the load-ratio-control autotransformer taps are automatically changed by the regulator.

D-C Switchgear

High-speed d-c switching, particularly where more than one rectifier supplies the same load, is considered a highly desirable application requirement. By high-speed switching is meant a d-c circuit interrupting device which will begin to open in one-half cycle or less, and will therefore limit the peak current, and will completely interrupt it in approximately one cycle. The primary oil switch in the supply circuit to the rectifier transformer will have a normal operating time from six to eight cycles and would permit arc-back currents to reach undesirably high values.

Two methods of high-speed d-c switching are in common use: The older method uses a high-speed cathode breaker which trips on reverse current and removes the faulty rectifier from the d-c bus. Since the short circuit still exists on the secondary circuit of the rectifier transformer, it is necessary to attempt either to extinguish the arc in the rectifier by ignition blocking, or to open the main oil switch.

The second method of applying high-speed switching is the use of the high-speed anode breaker as indicated in Figure 4 and Figure 4A. The use of the high-speed anode breaker is an important development in the application of rectifiers, and, while there have been some papers written on this subject, the authors feel that it is desirable to review briefly the reasons for their wide acceptance by the operators.

The use of an anode circuit breaker provides the best protection not only for the rectifier but for the process being supplied by direct current, since its use gives a greater guarantee of continuity of service. The high-speed anode circuit breaker has one pole placed in each anode circuit and replaces a single-pole high-speed cathode breaker.

Referring to Figure 4A, normal current flow at a given moment is indicated by the solid-line arrows. When an arc-back occurs, one of the anodes (the right-hand one, not supposed to be carrying current at that instant) allows current to flow in the reverse direction. It will be seen that an arc back therefore causes two short circuits, one on the d-c bus indicated by the "abnormal" dotted arrow marked *X*, and the other on the a-c system indicated by the "abnormal" dotted arrow *Y*.

It is desirable to remove these short circuits quickly. An interrupter in the

anode circuits removes the short circuits from the a-c and the d-c systems simultaneously.

The high-speed anode circuit breaker opens in response to the reverse direction of current in the anode. The current peak is limited to one-half cycle or less and the current completely interrupted in approximately one cycle.

In comparison, the high-speed cathode circuit breaker would only interrupt the feedback from the d-c bus into the faulty anode, but some additional means has to be used to remove the short circuit from the a-c system, either by opening the primary oil circuit breaker or by attempting to suppress the arc in the rectifier by ignition control, while the use of the high-speed anode circuit breaker goes directly to the source of the trouble and quickly and promptly interrupts the anode circuit. It should be noted that the use of an anode circuit breaker is not a substitute for a primary oil circuit breaker, but only a supplement of it. Transformer fault protection and normal switching generally require the use of primary circuit breakers when such large transformer unit is connected to an a-c bus.

Some of the specific advantages of high-speed anode circuit breakers, particularly in those cases where more than one rectifier supplies a common d-c bus, are:

(*a*). The transformer is protected against the high currents which may result when a rectifier arcs back. Such high currents impose very severe stresses on the transformer.

(*b*). The other rectifiers are protected against supplying large d-c currents to the faulty rectifier. Such protection against heavy currents reduces materially the tendency for the normal rectifiers to arc back "sympathetically."

(*c*). The remaining normal anodes on the same rectifier are not called upon to supply heavy currents to the faulty anode, and their tendency to arc back, either immediately or in the future, is greatly reduced. Arc-backs beget arc-backs.

(*d*). The use of anode breakers, with high-speed reverse-current trip on each pole, greatly simplifies the protective control system. There are no additional relays nor wiring to go wrong.

(*e*). When one rectifier transformer supplies two rectifiers, as is usually the case in electrochemical work, the use of high-speed anode circuit breakers will remove one of them on arc-back and permit the other to continue in uninterrupted service.

The principle of the high-speed anode circuit breaker has been well proven in service. It is so fundamentally a correct device for the protection of mercury-arc rectifiers that future electrochemical application without them will be rare indeed.

It will be noted from Figures 4 and 4A that a semihigh-speed cathode breaker is indicated. These semihigh-speed cathode breakers provide overload protection in the forward direction, since high-speed anode breakers which trip on reverse current are not particularly well suited to trip on straight overloads in the normal direction. The semihigh-speed cathode-breaker overload trip is given a slight time delay to permit the breaker to stay in until the high-speed anode breaker has cleared the arc-back. The semihigh-speed cathode breaker is also useful for normal emergency trip of the entire cell line.

Station Control

The station operator's control devices for controlling a station similar to that shown in Figures 3 and 4 are concentrated on an operator's control board.

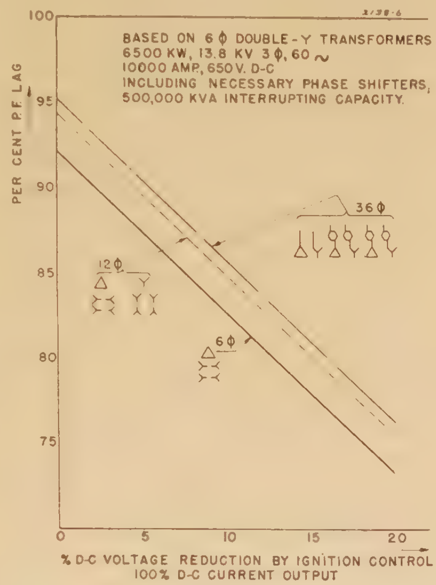


Figure 6. Power-factor characteristics for varying amount of grid control

The operator's functions in energizing such a cell line or taking it off the line are relatively simple. For energizing the cell line, the auxiliary circuits for water and vacuum pumps are energized, and then the a-c circuit breakers are closed. The anode and cathode breakers are next closed with the ignition circuit de-energized. The ignition circuit is then energized, permitting the rectifiers to take load. As previously mentioned, the d-c breakers cannot be closed with the ignition circuit energized, because the first breaker closed will trip again due to overload.

For some types of cell lines, and under some conditions, the operator may wish to start the cell line with the lowest possible d-c voltage permitted by ignition control, and energize the ignition circuit only after it has been retarded. All of these operations are performed at the operator's control board shown in Figure 3.

A cell line can be de-energized by simply tripping the cathode breakers. If one of the cathode breakers should lag materially behind the others, it will be forced to interrupt the entire cell-line current which represents an exceedingly heavy duty. If the operator desires and has the time, he may reduce the current interrupted by these breakers by first reducing the voltage and current on the entire cell line through the ignition control.

The power to cell lines should not be interrupted by de-energizing the ignition circuit. If this is attempted, the entire cell-line current somewhat attenuated will be finally transferred to a single anode. This is a serious overload for the particular anode and may cause serious damage to it. Even if all the cathode breakers are tripped simultaneously, they will open at slightly different moments. However, the magnetic energy stored in the cell-line circuit is dissipated quickly and without harm in the arc chutes of the cathode breaker.

Another method of de-energizing the cell line is to open the main a-c feeder breakers, a method which would correspond to the case of a complete power failure. Except for increased maintenance on the breaker, such interruption will not have any particularly bad effects, because the energy flow from the system is interrupted, and the current is transferred finally to the last conducting anode, only after it has died down to a relatively low and safe value.

Such stations as those illustrated by Figures 3 and 4 are continuously attended. The operations under normal circumstances are relatively simple and are usually confined to the observation of the instruments and the rectifiers. Where no automatic current nor voltage regulation is used, it is necessary to adjust the voltage from time to time by operating the tap changer or load-ratio control of the transformer, and perhaps occasionally to balance the load between the rectifiers by means of a slight amount of ignition control.

Temperature-Regulating Equipment

The losses in the rectifiers are usually removed by water cooling. There are a few installations of air-cooled rectifiers in Europe but no important installations in this country. In water-cooled rectifiers, the coolant is usually recirculated through some form of heat exchanger. Where the raw water supply is sufficiently cool,

clean, and abundant, these heat exchangers are generally water-to-water. Where the water supply is less favorable, these heat exchangers may be water-to-air.

The temperature-regulating heat exchangers as shown in the typical station layout of Figure 5 are usually located directly beneath the rectifier and the coolant continuously circulated between the rectifier and the heat exchanger. For most efficient rectifier operation, the temperature of this coolant is maintained at approximately 47 degrees centigrade. Thermostatically controlled valves on the heat exchanger maintain this temperature by automatically controlling the amount of raw cooling water flowing through the heat exchanger.

The recirculated liquid coolant is usually distilled water and may be treated with sodium dichromate to inhibit corrosion. These water-to-water heat exchangers can be manufactured for cooling water temperatures as high as 35 degrees centigrade (95 Fahrenheit). For a 36,000-kw rectifier installation considered in Figure 4, there would be required 420 gallons per minute of 85 degrees Fahrenheit raw cooling water.

In water-to-air heat exchangers, the coolant is also continuously circulated between the heat exchanger and the rectifier. A by-pass valve, thermostatically controlled, will pass more or less of the recirculated coolant through the heat exchanger while the fans run continuously.

Heat exchangers, when sodium-dichromate corrosion inhibitor is used, are at cathode potential and are therefore usually insulated from ground. Rubber hose is used to connect it to the rectifier and to the raw water supply.

The use of water-to-air heat exchangers, because of blower losses, may lower the station efficiency perhaps one tenth of one per cent, but since these blowers operate at full speed only a few months in the summer time, this is not an important consideration. The choice of water-to-water or water-to-air heat exchangers should only be considered from the standpoint of expense and quality of the raw water supply available. The installed cost of the water-to-water heat exchangers is considerably less than water-to-air, if the raw water is delivered to the building at no cost.

Station Arrangement

A typical station arrangement for a large electrochemical installation is shown in Figures 5A and 5B.

The transformers are installed outdoors and are usually of the oil-insulated self-cooled type.

The temperature-regulating heat exchangers are installed on the first floor of the main rectifier building directly below each of the rectifiers to which they are connected. For such large installations this arrangement gives the best economy of building space.

The rectifiers are mounted on the second floor at about the same elevation as the transformer secondary connections. Bus connections to the individual rectifier anodes are made preferably below the rectifier assembly. The cathode connections to the cathode breakers are also made from below. In this particular illustration, the neutral or negative connection from the interphase transformer is carried across the station directly below the roof.

The d-c breakers may be installed either on the ground floor or on the rectifier floor. An argument in favor of installing them on the rectifier floor is that the operator is then able to see that the breakers on a particular rectifier are open when work has to be done on this rectifier. However, space is generally available in the basement, and a narrower building results if these devices are placed in the basement.

On the second or rectifier floor of the building, is placed the operators' control board as illustrated in Figure 3. The control boards for two or even three pot-line equipments may be grouped together and under the control of one operator.

On the ground floor, aisle space is provided so that a portable degassing transformer may be moved from one rectifier to the other for initial degassing, or for degassing after a single unit has been returned to service after repairs.

The control cubicles containing the control devices for the pump and other auxiliaries for each rectifier are installed on the end of each rectifier.

The a-c switchgear in this particular illustration of Figure 5A is shown located in a separate building with the two autotransformers adjacent thereto.

It should be noted that this installation is typical of many electrochemical installations in that all of the 13.8-kv power cable enters the transformers and switchgear through pot heads, and there are no exposed 13.8-kv bushings in the system. This protection is a matter of some importance to those electrochemical installations where corrosive atmospheres or conducting dust is present.

A-C Power Supply

Reliability of power supply for electrochemical installations is considered essential, since loss of power even for a short time may be very costly, in addition to being naturally objectionable from the standpoint of the operator. In the case of chlorine cell lines, loss of power requires the immediate stoppage of auxiliary gas pumps and of the feed to the cells; otherwise chlorine gas may be liberated in the cell room with undesirable effects on the operators and the surrounding neighborhood. With aluminum and magnesium plants, the loss of power for an hour or more may cause the cells to freeze. In general, loss of power may mean an interruption of production for a much longer period than the duration of such power interruption and may require a costly and slow restarting of the process.

Therefore, the electric equipment and its power supply, which are the heart of this process, must be of the most reliable type and must include such spare capacity as may be indicated by experience. Even the best or most efficient or most reliable electric equipment will have an installed cost not usually exceeding 30 per cent of the total installed cost of the entire electrolytic plant which it supplies.

As shown in Figure 4, two autotransformers supply each pot line so that the reliability of power supply is insured, at least on a reduced basis if one autotransformer or its cable feed is lost. Each autotransformer has a continuous rating of two thirds of a pot line.

By proper separation of circuits, even these large electrochemical installations may be kept within a 500,000-kva short-circuit capacity breaker. Normal distribution voltage in well over 90 per cent of the electrochemical installations in the United States is 13.8 kv.

The double bus systems shown on the output side of the autotransformers permit any of the six main rectifier transformers to be connected to either of the autotransformers. While not indicated in Figure 4, the double bus system permits the autotransformers to be connected to different bus sections with no interconnections between feeders. This would be particularly important, if each autotransformer is fed from a different bus section in the powerhouse, and these powerhouse bus sections are tied together through a synchronizing or transfer bus. The fact that there is no interconnection on the output side of the autotransformers avoids any possibility of by-passing the synchronizing bus.

The outdoor substation supplying the three pot lines should logically have 45-degree transformers, to be consistent with the rectifier transformer practice. In the arrangement shown in Figure 4, the main stepdown transformers are provided with

fans, so that, in the event of the loss of one bank, the fans can be started on the remaining two banks, and power transferred to them through the transfer bus.

The transfer bus is normally disconnected and the bus sections A, B, and C are independent, thus localizing the effects of short circuits and other disturbances.

Because of the current-carrying capacity required on the main outdoor transformer bank, Figure 4 indicates that the delta for the secondary windings of these transformers is made on the metal-clad bus by using two 2,000-ampere, triple-pole breakers. This is a method frequently resorted to in order to obtain the necessary current-carrying capacity in metal-clad switchgear.

The system should, of course, be supplied with two-incoming high-voltage lines, in accordance with the standards of reliability required. Since interruptions on the incoming high-voltage lines are the most frequent, the cost of high-voltage circuit breakers and protective relaying is justified.

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Factors Affecting the Mechanical Deterioration of Cellulose Insulation

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Synopsis: The rate of mechanical deterioration of cellulose insulation is dependent on the conditions of its use. Those factors of major importance are the temperature applied and the presence of oxygen and moisture. Moisture even in small amounts greatly affects the mechanical stability of the cellulose insulation. In general, the mechanical life of the insulation is reduced by half for each doubling in water content. Deterioration promoted by oxidation is most effective at temperatures below 120 degrees centigrade and is accelerated by the presence of moisture.

The rate of deterioration for substantially dry insulation at temperatures above 120 degrees centigrade is dependent upon its previous history. Intermittent exposure to high temperature effects are additive. The "eight-degree-centigrade rule," indicative that the rate of mechanical deterioration is doubled for each eight-degree-centigrade increase from a base temperature of 120 degrees centigrade or higher, applies most closely for practical use when the insulation under study has lost more than 50 per cent of its tensile strength.

CELLULOSE insulation is widely used as the dielectric in high-voltage electric apparatus. Like mineral oil with which it is usually associated, cellulose in commercial practice is subject to chemical change to an extent which is dependent upon the conditions of its use. The deteriorating effects of temperature and oxygen are generally recognized and have been the subject of exhaustive research and discussion. The effect of moisture, as described in this paper, has not been carefully evaluated in previous studies of cellulose stability, despite the fact that it appears to play a dominant role in the chemical and mechanical changes exhibited by the cellulose when exposed to temperatures within the range encountered in the commercial use of transformers and other high-voltage electric apparatus.

As a material for insulating electric apparatus cellulose is used in a variety of forms. In its widest application cellulose is used in the form of paper sheets or tapes for the insulation of conductors which may vary greatly in size and shape. In some applications it is subjected to relatively low electric stresses and serves chiefly as a spacing material for the separation of adjacent conductors. In other applications it is subject to severe electric stress which in the case of capacitors and similar apparatus may be as high as 300 or 400 volts per mil. In most of its uses the cellulose is impregnated with mineral oil or varnish or both. It may, on the one hand, be found in apparatus operating under conditions allowing more or less free access of air or, on the other hand, in apparatus where contact with air or other source of oxygen is substantially eliminated by the use of hermetically sealed containers or by other means. In all of its applications, however, it is essential that chemical deterioration of the cellulose, resulting in the loss of dielectric efficiency and mechanical strength, shall be completely eliminated or carefully controlled in order that the insulation may be able to withstand the severe electric and mechanical stresses set up, not only in the normal use of the apparatus but also under short circuit or overload conditions.

One of the most difficult of all engineering problems is the translation of laboratory "life" data into terms of practical usage. The present paper concerns itself entirely with laboratory data correlating those factors of time, temperature, moisture, and oxidation which have been found to be important in affecting the mechanical stability of cellulose insulation. The results obtained are reported in order to assist the engineer in his efforts to use materials efficiently. The relative importance of the deteriorating factors investigated can only be finally determined from a study of the results obtained under the conditions of commercial use.

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Objects of Investigation

In a previous paper[1] it has been related that the chemical and mechanical deterioration of cellulose is a complicated phenomenon greatly influenced by the decomposition products formed as a result of the chemical changes involved. Among those products are organic acids, water, hydrocarbons and their derivatives, and gases such as the oxides of carbon. It has been demonstrated that once initiated the chemical changes involved are accelerated to a degree dependent on the retention of the decomposition products. The present paper has as one of its objects the evaluation of the influence exerted by the presence of moisture on the mechanical stability of cellulose when aged under conditions which do not allow the escape of the deterioration products formed.

It has already been shown[2] that cellulose sheets or tapes will deteriorate mechanically when heated even in the absence of oxygen. The pyrochemical effect as contrasted to the effects of severe oxidation become pronounced as the temperature is raised above 120 degrees centigrade. Cellulose when heated passes through a "stable period" during which the mechanical properties are maintained. The presence of oxygen is chiefly effective in reducing the duration of this "stable period." It is a further object of this paper to demonstrate the effect of moisture on the duration of this "stable period."

Cellulose insulation as in transformers is subject to wide variations in temperature. Under overload conditions localized temperatures are obtained far in excess of those normally associated with the daily use of the apparatus. Engineering practice is to assume that the effects of such abnormal temperatures are additive. It is another object of this paper to examine the validity of such an assumption.

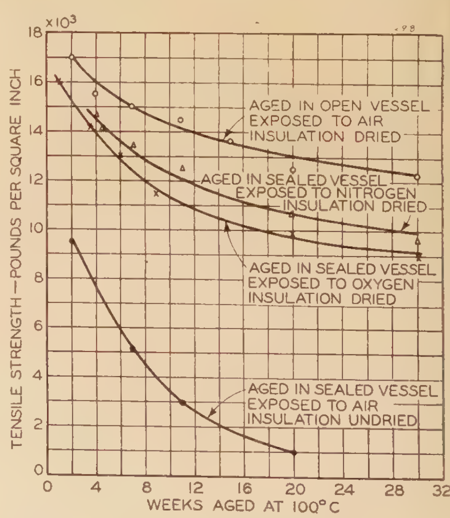


Figure 1. Showing the effect of testing conditions on the mechanical stability of 0.003-inch Manila insulating paper when aged under mineral transformer oil at 100 degrees centigrade

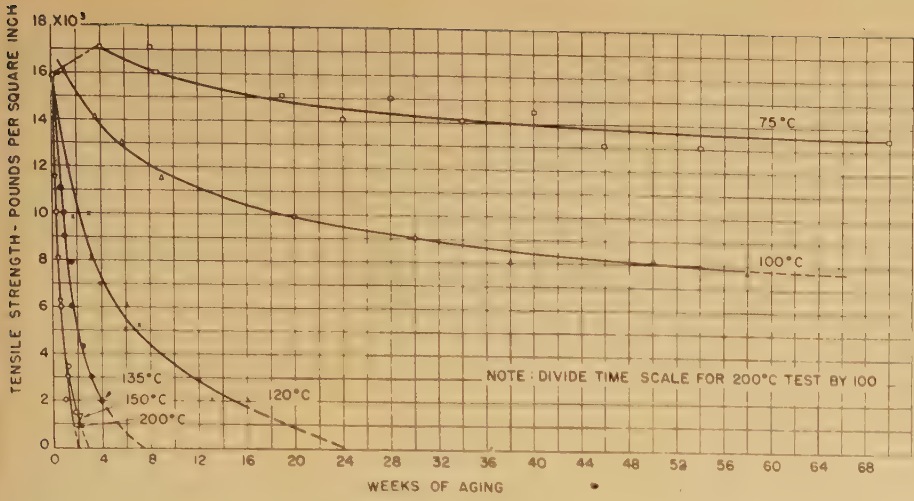


Figure 2. The tensile strength of vacuum-dried and oil-impregnated 0.003-inch Manila insulating paper as affected by aging in sealed containers under oil, the surface of which was in contact with oxygen gas

Chemical reactions involving organic materials are accelerated by heat. In a general sense, it is accepted that such chemical reactions are increased twofold for each ten-degree-centigrade increment in temperature. Montsinger in previous publications[3,4] has called attention to the importance of properly evaluating the influences of temperature as a criterion of the ability of transformer insulation to withstand the effects of temporary overloads encountered in the use of transformers. He has suggested the "eight-degree rule," indicative that the rate of the mechanical deterioration of cellulose doubles for each eight-degree-centigrade rise in temperature. It is a final object of this paper to demonstrate the rate of cellulose deterioration as affected by temperature increase under widely different conditions of possible use.

Testing Technique

Experience in the study of cellulose deterioration at elevated temperature demonstrates the necessity of closely controlling all those factors which have long been recognized as of fundamental importance in the study of sludge formation in mineral transformer oil.[5,6] The temperature must be carefully controlled. In order to obtain duplicate test results, the temperature should be controlled to ± 0.5 degree centigrade.

The testing receptacle in which the cellulose is aged must be carefully selected if reproducible results are to be obtained. Access of air or other gases must be controlled. The ease of escape of products formed as a result of the deterioration must be clearly described and evaluated in terms of practical usage. The widely different results (Figure 1) accompanying a change in the conditioning treatment of the insulation or in the design of the vessel used for the aging only serve to emphasize the confusion which may arise when details of this type are ignored.

In the "sealed-tube" tests hereinafter described, the tube is of Pyrex glass and measures 200 millimeters in length and 25 millimeters in diameter. When sealed it has a volume of 59 milliliters. The conditioned oil and the oil-immersed paper insulation fill the tube to the 50-milliliter mark, the remaining 9 milliliters being filled with the stated gas conditioned as described. The paper used is cut before conditioning in strips, each one-half inch wide and ten inches long. The paper used throughout this study is 0.003-inch Manila cable paper. Ten strips of such paper, conditioned as described, are used in each test. This gives a ratio of approximately one-quarter pound of paper per gallon of oil. The tensile strength of the paper is determined in a room maintained at 65 per cent relative humidity, 70 degrees Fahrenheit. The oil-impregnated papers are tested immediately after having been taken from immersion in the oil. The tensile strength is determined in a Schopper testing apparatus. The results reported are the average of ten individual tensile strength tests.

The mineral oil used throughout this paper is a typical American transformer oil (58 seconds Saybolt Universal viscosity at 37.8 degrees centigrade) and is manufactured from Gulf Coast crude. Before use it is completely degassed and dried. During test it is saturated with the gas with which it is in contact, as is described in each particular instance.

Moisture Determination

The problem of determining the moisture content of cellulose sheets has been recognized as difficult and subject to variation because of the instability of the cellulose itself. Urquhart and Williams[7] have determined that oven drying at 110 degrees centigrade are as satisfactory as vacuum drying over phosphorous pentoxide at 15 degrees centigrade. Such methods may be well suited for roughly determining the water content of cellulose containing large amounts of moisture, such as is present in ordinary paper exposed to atmospheric conditions, but they fail to give accurate test values when the water content of the paper is materially reduced.

A satisfactory method of test for determining the water content of insulating papers has been established as follows:

Approximately 50 grams of paper, preferably cut into pieces about three by three-fourths inches in size (less weight of paper if suspected to be high in water content, more weight if suspected to be of low water content) is immersed quickly in 500 milliliters of dry liquid such as mineral transformer oil contained in a thoroughly dry 1,000-milliliter round bottom flask. Dry nitrogen gas is passed through the liquid at the rate of 100 milliliters per minute with a fritted gas washing tube to obtain the best dispersion. The temperature of the liquid is raised to 110 degrees centigrade plus or minus two degrees. The gas after passing from the liquid-paper suspension is passed through two liquid air-cooled traps connected in series where the moisture and other volatile materials are condensed. The outlet for the nitrogen gas is protected by a conventional drying tube to prevent suck back of atmospheric moisture. The nitrogen gas is passed through the test flask for three hours after which the water collected in the liquid air traps is quantitatively determined. The nitrogen gas treatment is repeated in three-hour steps at 110 degrees centigrade until all water has been driven from the paper under test. The quantitative test for the water collected is carried out as follows:

Ten milliliters of an acetyl chloride solution in dry benzene (118 grams per liter of solution) are added to a dry 250-milliliter glass stoppered flask and cooled in an ice bath. Two milliliters of dry pyridene are added dropwise to the acetyl chloride solution with continual shaking. A white precipitate of acetyl chloride-pyridene complex is formed.

The frozen condensate in the traps is dissolved in 25 cubic centimeters of dry acetone and added to the cold acetyl chloride-pyridene complex. The acetone is added in 5-5-2.5-2.5 milliliter portions to the first trap and in 5-2.5-2.5 milliliter

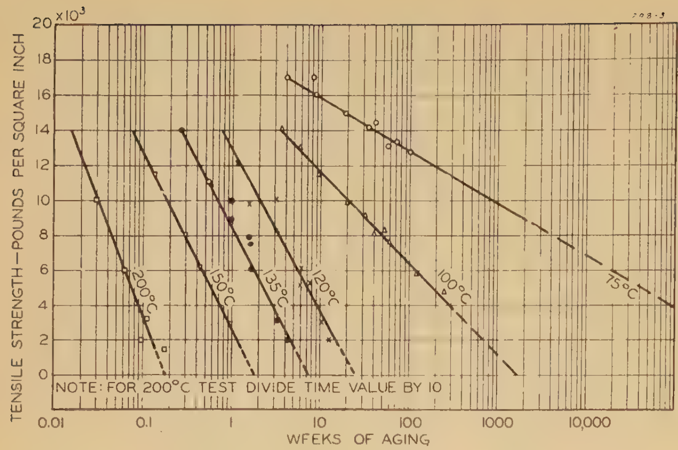


Figure 3. The tensile strength of vacuum-dried and oil-impregnated 0.003-inch Manila insulating paper as affected by aging in sealed glass containers under oil, the surface of which was in contact with oxygen gas

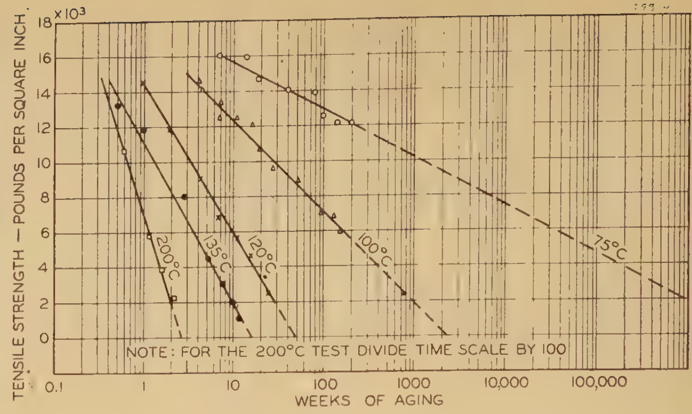


Figure 4. The tensile strength of vacuum-dried and oil-impregnated 0.003-inch Manila insulating paper as affected by aging in sealed glass containers under oil, the surface of which was in contact with nitrogen gas

portions to the second trap. The stoppered flask is then removed from the ice bath and allowed to stand at least 15 and not more than 30 minutes with occasional shaking out of contact with direct sunlight. Four milliliters of dry ethyl alcohol are added and the mixture is well shaken. Sixteen milliliters more of dry ethyl alcohol are then added and after being shaken and allowed to stand for five minutes, the mixture is titrated with 0.5 normal sodium hydroxide to a phenol phthalein end point. A blank test is run with all conditions identical in order to correct for residual moisture in the reagents and apparatus. The amount of moisture in the paper, expressed in terms of its dry weight is then calculated.

Using this procedure definite and reproducible test results have been obtained. Care must be taken, however, to avoid decomposition and subsequent removal of the "water of constitution" from the cellulose sheet. If the analysis be carried beyond the point at which the water evolution from the paper drops to zero, subsequent heating will result eventually in some decomposition of the cellulose.[1] The formation of water as a result of this decomposition will obscure the real moisture content determination.

The determination of the moisture content of a cellulose sheet is difficult and fraught with many technical pitfalls. The method described gives results which appear lower than those heretofore reported for mineral oil-treated cellulose insulation.[11] Pending further experimental data, the moisture contents described in subsequent paragraphs might well be considered as "relative" rather than as fixed and absolute values. Such a precaution in no wise diminishes the importance of the studies demonstrating the marked effect of moisture on the mechanical life of the cellulose insulation.

Expression of Test Results

Because of its greater degree of reproducibility, the tensile test has been adopted as the gauge of mechanical strength of the cellulose sheet and its rate of deterioration. Typical time relationships are illustrated in Figure 2. Because of the difficulty in clear presentation, however, such relations are not generally used in this paper. The semilogarithmic relationship between the tensile strength and the duration of aging have been found to be better suited for clear presentation (Figure 3).

The Effect of Temperature

There are many technical processes which employ cellulose products at high temperature. This has led to the initiation of many researches concerned with the stability of cellulose products over short periods of heating. The general conclusion reached has been that cellulose suffers some decomposition at temperatures above 150 degrees centigrade. The problem of the insulation engineer however is the problem of time and temperature. The stability of cellulose during short periods of heating[1,2] are frequently misleading. Knecht[8] has shown that, when cotton is heated for 336 hours at 80 degrees centigrade, chemical changes occur which endow the cellulose with reducing powers. It is the effect of these often overlooked chemical changes, culminating in mechanical deterioration, with which this paper is concerned. The time of heating is obviously of importance.

The effect of time and temperature over the range from 75 to 200 degrees centigrade on the tensile strength of vacuum-dried and oil-impregnated 0.003-inch Manila paper is illustrated in Figure 2. During the aging runs the insulation was immersed in dry oil in sealed Pyrex glass tubes, the surface of the oil being in contact with oxygen as already described. The semilogarithmic relation of tensile strength and time of heating is illustrated in Figure 3. Figure 4 illustrates similar data for vacuum-dried, oil-impregnated 0.003-inch Manila paper, the surface of the oil in which the paper is immersed being in contact with dry nitrogen gas. The marked effect of temperature on the mechanical stability of Manila paper is illustrated in Figure 5 which shows the "50 per cent life" in tensile strength over

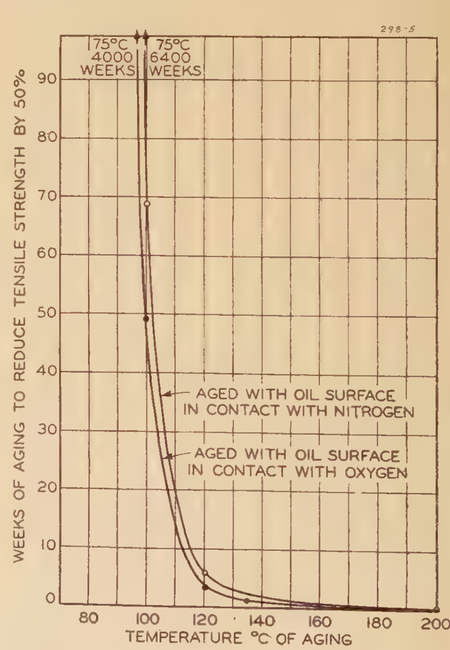


Figure 5. Showing the marked effect of temperature increase above 75 degrees centigrade in the mechanical life of vacuum-dried and oil-impregnated 0.003-inch Manila insulation paper when aged in sealed glass containers under oil, the surface of which was in contact with oxygen or nitrogen gas as indicated

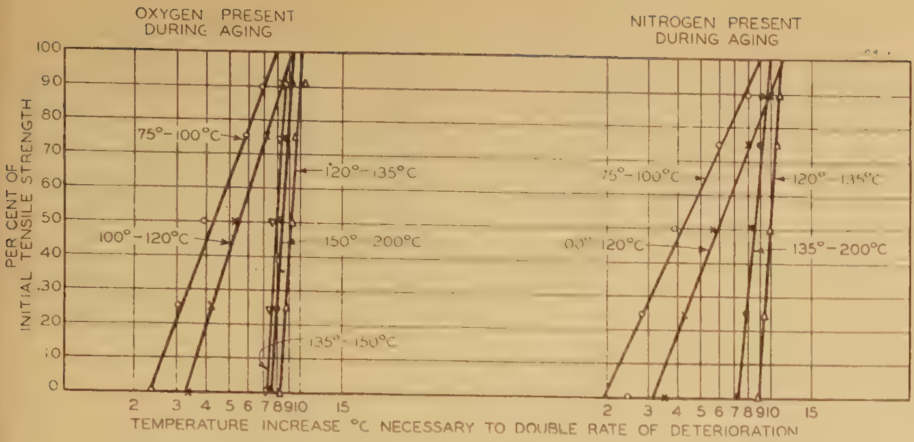


Figure 6. The rate of mechanical deterioration of vacuum-dried and oil-impregnated 0.003-inch Manila paper becomes more sensitive to temperature increase as the deterioration progresses

The effect is most pronounced when the temperature of test is below 120 degrees centigrade

the temperature range from 75 to 200 degrees centigrade. It is clearly evident that as the temperature rises above 75 degrees centigrade the deterioration proceeds at a rapidly increasing rate. It is the proper evaluation of this rapidly increasing rate of deterioration which is of fundamental importance in the application of cellulose insulation in high-voltage electric apparatus.

Inspection of the data presented in Figures, 2, 3, 4, and 5 at once leads to the conclusion that the rate of mechanical deterioration of Manila insulating paper cannot be expressed in terms of any single temperature expression. Figure 6 plots as a semilogarithmic expression the relation between the deteriorating mechanical strength and that temperature rise which is necessary in order to produce a twofold increase in the mechanical deterioration over the temperature range shown. For temperatures in the range from 75 to 100 degrees centigrade the effect of temperature change is extremely marked, whether the deterioration be produced in the presence or absence of oxygen. In this range, an increase in temperature of but 2.4 degrees centigrade will reduce the total life of the insulation by half. With temperatures of 120 degrees centigrade and higher, however, the deterioration is less sensitive to temperature increase. To double the rate of deterioration for these higher temperatures (reduce the total life by half), an increase ranging between approximately seven and ten degrees centigrade is necessary. The relation of the temperature of the aging to that temperature increase necessary to produce a twofold rate of deterioration when the tensile strength has been reduced to 50 per cent and zero per cent of its initial value is illustrated in Figure 7. The so-called "eight-degree rule" suggested by Montsinger is a good practical compromise for the higher temperatures of use (120 degrees centigrade and higher) when the tensile strength has been reduced to less than about 50 per cent of the initial value.

The Effect of Oxygen

The problem of evaluating the practical effect of oxygen on the mechanical strength of cellulose insulation during the normal use of a transformer or similar apparatus is difficult, because of the wide differences in transformer designs. The transformer designs of greatest importance for consideration, however, are the hermetically sealed type containing a gas cushion and the conservator type. Each of these has a limited supply of available oxygen. In the conservator transformer the amount of oxygen is limited to that carried into the transformer as an air solution in the mineral oil. In the hermetically sealed transformer the amount of oxygen is limited to that present in oil solution and in the gas cushion when the transformer is sealed. When this gas cushion consists of nitrogen gas, the amount of oxygen available within the transformer is negligible if the mineral oil is properly deaerated before the transformer is sealed.

In the laboratory study of the effects of oxidation on the mechanical strength of the cellulose insulation, an attempt has been made to exaggerate the hermetically sealed transformer design. The sealed tubes have been filled with oil and the oil-immersed insulation to 85 per cent of the volume. The gas cushion occupies the remaining 15 per cent of the volume. These are the approximate relationships of oil to gas volumes in the average hermetically sealed transformer. The effect of oxidation has been exaggerated by the use of pure oxygen in the gas space. Pure oxygen has been used to exaggerate the oxidation possibilities, since in a previous publication[2] the presence of air has been demonstrated to be of negligible oxidation effect within the duration of the laboratory tests made. The use of this type of test arrangement is considered to be more severe than the conditions which are met in either the conservator or the hermetically sealed type of oil-filled transformer.

It has been suggested[10,12] that the effect of oxygen on the mechanical deterioration of oil-treated cellulose insulation involves consideration of the acids formed as a result of the oxidation of the oil itself. In a general sense this is true. Therefore, since the most corrosive acids formed both from the oil oxidation and from the oxidation of the cellulose are volatile at the temperature of the tests described, sealed containers have been used for the aging study. This insures that all products of decomposition and oxidation, whether from the oil or from the cellulose, are retained within the test receptacle. From the practical standpoint this procedure is justified, since the mechanical deteriora-

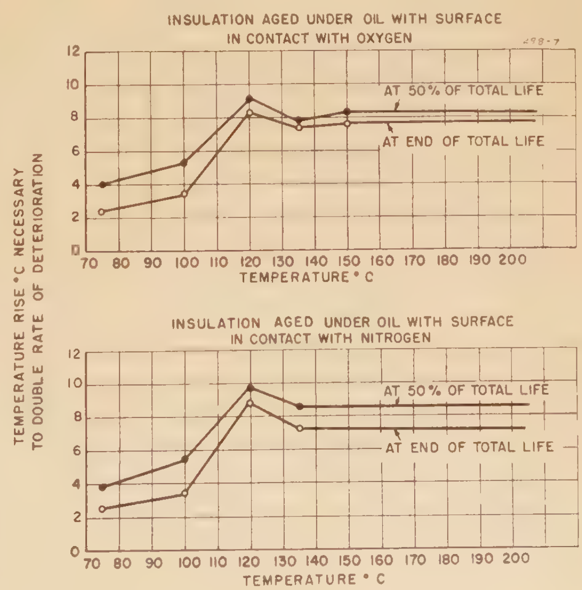


Figure 7. The "eight-degree rule" correlating temperature increase and mechanical deterioration of vacuum-dried and oil-impregnated 0.003-inch Manila paper applies with practical accuracy when the paper has deteriorated to at least 50 per cent of its initial value at temperatures of 120 degrees centigrade and higher

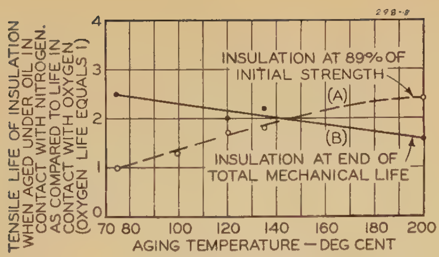


Figure 8. The elimination of oxygen increases the initial stability of vacuum-dried and oil-impregnated 0.003-inch Manila paper over the temperature range 75–100 degrees centigrade

The total life of the insulation, however is progressively less affected by the elimination of oxidation as the temperature of the aging is increased

tion reported therefore includes consideration of the accumulated effects of all the oxidation and decomposition that occurs.

In a previous publication[2] it was concluded that the "single effect of oxidation was restricted to the initial periods of treatment at temperatures lower than 120 degrees centigrade. At higher temperatures pyrochemical decomposition occurs . . ." The data presented extend and support this conclusion and indicate that the major effect of oxidation is produced when the insulation is exposed to the lower testing temperatures. At 75 degrees centigrade the deterioration due to oxidation becomes more pronounced as the mechanical aging progresses. At higher temperatures the pyrochemical decomposition of

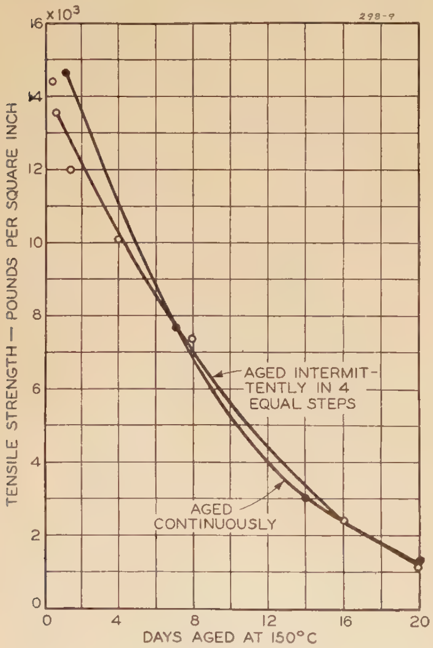


Figure 9. The mechanical aging of vacuum-dried, oil-treated, and oil-immersed 0.003-inch Manila insulating paper at 150 degrees centigrade in the presence of nitrogen gas is an additive function of the total period of heating when intermittently applied

the cellulose tends to reduce the effects of oxidation.

Figure 8 illustrates the effect of oxidation on the deterioration of the vacuum-dried and oil-impregnated 0.003-inch Manila paper for two degrees of deterioration. Curve *A* illustrates the increased life of the insulation which is obtained as a result of the elimination of oxidation when the mechanical strength has been reduced to 89 per cent of its initial tensile strength. This value corresponds roughly to the end of the "stable period." The effect of oxygen elimination increases with the temperature of the exposure. This supports the conclusion that the duration of the "stable period" is reduced by oxidation processes. When, however, the total life of the insulation is considered (tensile strength zero), curve *B* supports the conclusion that the occurrence of pyrochemical decomposition during the later stages of the cellulose aging becomes more pronounced with the higher testing temperatures and reduces the beneficial effects normally associated with the elimination of oxygen.

Intermittent Aging

The total aging of cellulose insulation when exposed intermittently to high temperatures necessarily includes the deterioration which occurs during the heating and cooling periods. To determine whether the intermittent application of high temperature is strictly additive, the insulation contained in the sealed glass tubes was heated as rapidly as possible by immersion in an oil bath maintained continuously at 150 degrees centigrade, and cooled rapidly by transfer to an oil bath maintained continuously at 25 degrees centigrade. This reduced the effect of the heating and cooling periods to a minimum. The effect of such intermittent heating and cooling on the tensile strength of vacuum-dried, oil-impregnated, 0.003-inch Manila insulating paper aged under mineral transformer oil at 150 degrees centigrade is illustrated in Figures 9 and 10. The intermittent aging relation was obtained by heating the insulation at 150 degrees centigrade for four equal periods of time. Between these heating periods the insulation was maintained at 25 degrees for 24 hours. In the absence of oxygen, the effect of intermittent heating has been found to be strictly additive within the limits of experimental error (Figure 9). In the presence of oxygen (Figure 10) the additive effect again appears to be within the limits of experimental error until the insulation has lost approximately 50 per cent of its initial tensile strength. For a

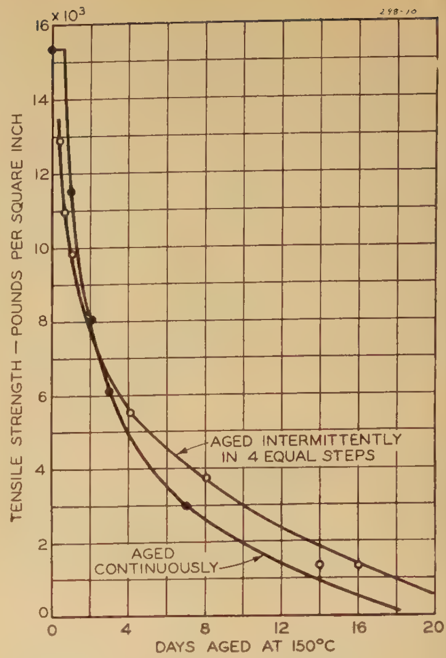


Figure 10. The effect of intermittent heating at 150 degrees centigrade on the mechanical strength of vacuum-dried, oil-treated, and oil-immersed 0.003-inch Manila insulating paper in the presence of oxygen

greater degree of deterioration the effect of intermittent aging appears less than additive, although the differences observed do not appear of major significance. Pending further investigation, it is concluded that the results obtained justify the engineering practice which considers the effects of intermittent heating at high temperature to be additive.

The Effect of Moisture on the Tensile Strength

Inspection of the data presented (Figure 6) correlating the effect of temperature on the rate of mechanical deteriora-

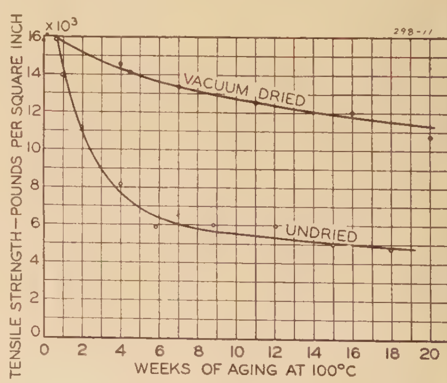


Figure 11. Showing the effect of moisture retained by cellulose in its normal condition on the mechanical stability of 0.003-inch Manila insulating paper during an aging run at 100 degrees centigrade in sealed glass tubes

During the aging the insulation is held under mineral transformer oil, the surface of which is in contact with nitrogen gas

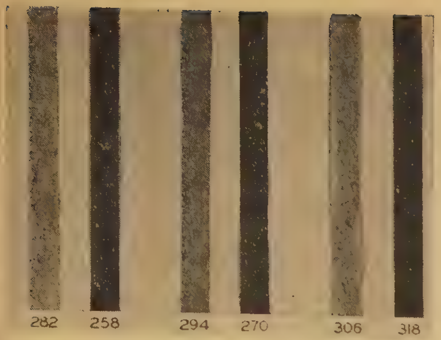


Figure 12. Illustrating the color darkening which accompanies the mechanical deterioration obtained when 0.003-inch Manila paper is aged in sealed tubes for two weeks at 120 degrees centigrade in the presence of moisture

Samples 282, 294, and 306—paper aged after having been vacuum dried and impregnated with mineral transformer oil, the surface of which is in contact with oxygen, nitrogen, and air respectively

Samples 258, 270, and 318—paper aged immersed in mineral transformer oil without previous drying, the oil being in contact with oxygen, nitrogen, and air respectively

tion leads to the observation that as the deterioration progresses it becomes more sensitive to temperature change. Thus, although in the early stages of heating, the paper deterioration increases approximately twofold for each seven to ten degrees centigrade rise in temperature, when the tensile strength has been reduced to as low as 25 per cent of its initial value, the rate of deterioration doubles for an increase of only about 2.4 to 9 degrees centigrade, depending on the testing temperature. This acceleration of the deterioration has been traced[1] to the effect of decomposition products retained in the body of the cellulose. Among such decomposition products is water. Because of the ability of water through ionization and otherwise to activate many of the products (acids and acidic materials) formed when cellulose undergoes deterioration, it is not unexpected to find that the mechanical strength of cellulose insulation is affected by the presence of moisture in even small amounts. Figure 11 illustrates the greater stability in the tensile strength of 0.003-inch Manila insulating paper which is obtained when the insulation is freed of its moisture before exposure to high temperature. In one instance the paper is heated under oil at 100 degrees centigrade without vacuum drying. In the other instance the paper is first vacuum dried to a low moisture content. The greater mechanical instability of the undried paper is accompanied by other visual evidence of greater chemical change. In contrast to the dried paper, the undried insulation after aging is darkened, reacts acidic, and smells strongly of a caramel odor, characteristic of overheated cellulose. Figure 12 illustrates the marked darkening in color which accompanies the greatly accelerated mechanical deterioration in the cellulose sheet when aged in the presence of moisture, even though the possibility of oxidation be eliminated by the use of nitrogen gas.

The data of Figure 11 are obtained as a result of an aging run in sealed glass vessels. It is suggested that the extreme variations reported in Figure 1 are due to the difference in the design of the aging vessel. On the one hand, the moisture and other volatile products originally present or formed during the test were allowed easy escape since the vessel was open to the air. On the other hand, the moisture and volatile products were completely retained in the sealed tube during the aging run. The wide difference in the test results reported in Figure 1 and in Figure 11 illustrates the wide variation which can be expected when the insulation is aged without careful attention to all of the conditions affecting the chemical stability of the cellulose. Not the least of these conditions is its moisture content.

The Effect of Moisture on the Rate of Deterioration as Affected by Temperature and Oxidation

The presence of moisture in cellulose insulation is of such importance in its effect on the mechanical stability at high temperature that it tends to modify the relationships set up for the dried insulation. To illustrate this, 0.003-inch Manila insulating paper containing 1.5 per cent moisture has been immersed in oil and aged at 100 and 120 degrees centigrade in sealed glass tubes of the type already described, the surface of the oil being in contact with oxygen or nitrogen gas. The results obtained are illustrated in Figure 13.

It has already been shown in Figure 8 that with dry insulation the presence of oxygen produces its maximum effect on the total life of the insulation when the aging is carried out at the lower testing temperatures. At 75 degrees centigrade the total life is increased about 2.5 times when an inert gas such as nitrogen is substituted for oxygen, thereby eliminating substantially all of the deterioration attributed to oxidation. When, however, there is a substantial amount of moisture present, its deteriorating influence accentuates the effect of oxidation. Figure 14 based on the data of Figure 13 demonstrates that in the presence of moisture the elimination of the oxidation reaction at 100 degrees centigrade has a more pronounced effect than is associated with elimination of oxygen on the life of the dried paper insulation.

In like manner, the effect of temperature increase on the aging of Manila insulating paper is materially modified by the presence of moisture. The deterioration becomes less sensitive to temperature change. This is illustrated in Figure 15.

Quantitative Effects of Moisture

The quantitative study of the effects of moisture on the mechanical aging of cellulose is made difficult by the fact that the equilibrium which exists between the amount of water retained by the cellulose and that present in the mineral transformer oil in which it is immersed shifts with changes in temperature.[9] The higher the temperature, the less water is retained by the cellulose. In the data relating to the water content of the cellulose in this and subsequent sections, the value given is the total water content of the oil and cellulose insulation within the sealed container at 25 degrees centigrade. At this temperature under the experimental conditions existing, substantially all of the water present is held by the cellulose and is so reported.

In order to eliminate the effects of oxidation which vary with the temperature of aging and the moisture content of the cellulose insulation, a typical set of test results, describing the deterioration of oil-impregnated and oil-immersed 0.003-inch Manila paper aged at 120 degrees centigrade in the presence of nitrogen, is selected to demonstrate the influence of

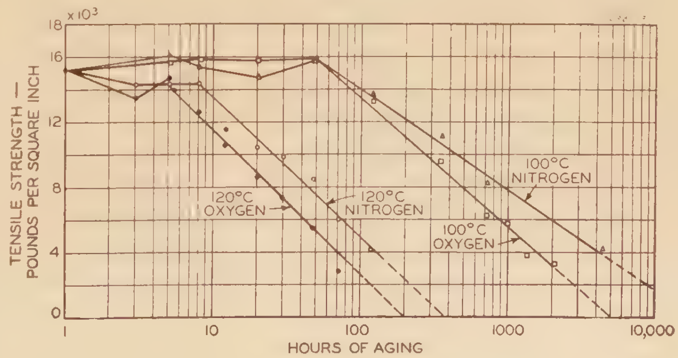


Figure 13. The aging of 0.003-inch Manila insulating paper containing 1.5 per cent moisture

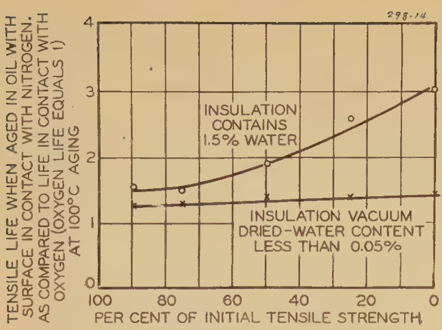
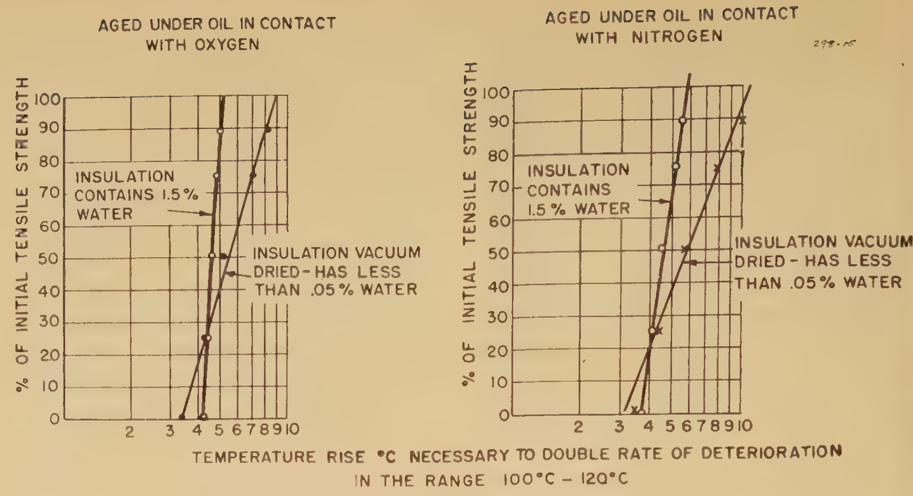


Figure 14. The importance of oxidation in promoting the mechanical deterioration of 0.003-inch Manila insulating paper is materially increased when the aging of the oil-treated and oil-immersed insulation is carried out in sealed tubes in the presence of moisture



moisture. The insulation was first vacuum dried at 100 degrees centigrade for a period of time ranging from less than one hour to more than 100 hours, after which it was impregnated with gas-free, dry mineral transformer oil and sealed at once in glass vessels partially filled with the oil and oil-immersed insulation as already described. The gas with which the oil was saturated and with which the oil surface was in contact was dry nitrogen. The water content of the assembly was determined at the start of each aging run. The effect of moisture was marked. The total life of the insulation was reduced from a total of 3,700 hours (22 weeks), characteristic of the well-dried insulation, to a life of less than 100 hours (0.6 week), when the paper was substantially undried and contained about eight per cent moisture. Figure 16 represents the relationship found to prevail in a typical series of test results obtained simultaneously. As illustrated in Figure 17, there exists a logarithmic relation between the water content of the cellulose insulation and that period of time necessary to produce a given amount of mechanical deterioration in the cellulose sheet when aged at 120 degrees centigrade under the conditions applied.

In a previous paragraph it has been demonstrated that the effect of oxidation during the mechanical aging of cellulose insulation materially shortens the "stable period" during which the insulation can be heated at high temperature without evidence of mechanical deterioration. The effect of the initial water content of the cellulose of this "stable period" has been found to be negligible. In Figure 16 the stable period is shown to be approximately 40 hours, irrespective of the initial water content of the Manila insulating paper. Since oxidation is effective in determining the length of this "stable period" it is at once obvious that other testing factors which affect the oxidation

Figure 15. The presence of moisture in incompletely dried, oil-treated and oil-immersed 0.003-inch Manila insulating paper exaggerates the effect of temperature increase from 100 to 120 degrees centigrade during the major part of the mechanical life

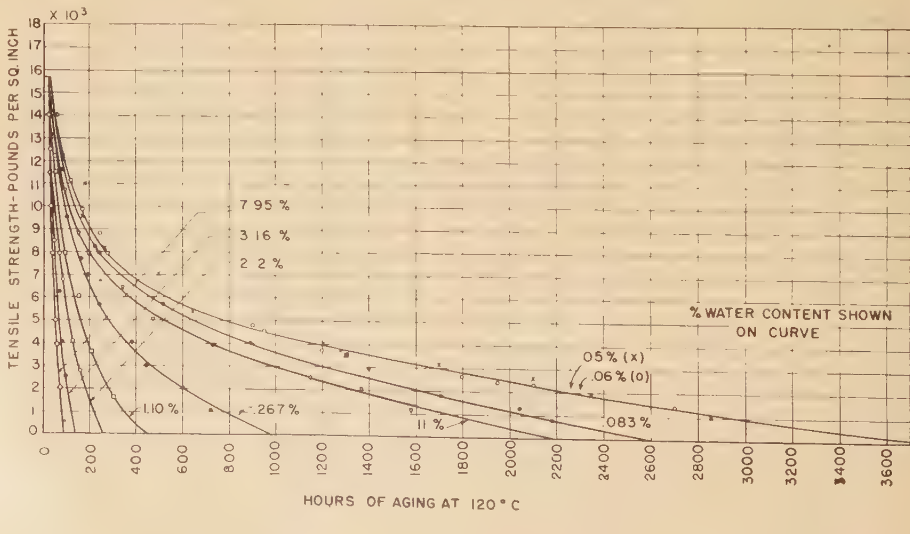
The greater initial resistance to temperature increase characteristic of the well-dried insulation, however, is reduced as the result of water formation as the deterioration progresses

reaction are of importance in determining its duration.

Because of the importance of mechanical stability in many of the dielectric applications of cellulose in electrical apparatus, the marked influence of moisture on the mechanical life of the insulation assumes practical value. Cellulose is a hygroscopic material and rapidly absorbs moisture from the surrounding medium.[9] The absorption of this moisture is effective in determining the subsequent rapidity in mechanical deterioration. From the data of Figure 16 it is obvious that, when oxidation is eliminated, the absorption of even small amounts of moisture by the dry insulation will materially reduce its mechanical life. Conversely, it is obvious that the absorption of relatively large amounts of moisture will have less effect on the aging of the improperly dried insulation. The relationship is illustrated in Figure 18.

To double the rate of mechanical deterioration in oil-treated and oil-immersed Manila paper in the absence of oxidation, the amount of moisture addition necessary is dependent upon the degree of deterioration which is selected for study. The total life of the insulation is halved (the deterioration rate doubled) by the addition of water equal to approximately 100 per cent of that originally present in the insulation. Thus, with a water content of 0.05 per cent, the life of oil-treated and oil-immersed Manila paper in the absence of oxygen is approximately 4,000 hours. With the absorption of an equal amount of moisture to give a total water content of 0.10 per cent, the total life of the insulation at 120 degrees centigrade is reduced to about 2,000 hours. On the other hand, to reduce by half the total

Figure 16. The effect of moisture content on the mechanical life of oil-impregnated and oil-immersed 0.003-inch Manila insulating paper when aged at 120 degrees centigrade in sealed glass containers, the oil surface being in contact with nitrogen gas



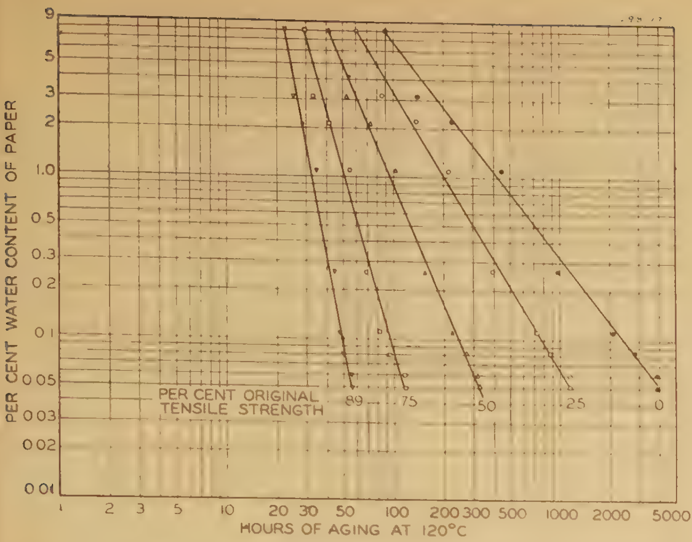


Figure 17. Under the conditions of test applying for the data of Figure 13, the water content of the cellulose insulation bears a logarithmic relation to the period of heating necessary to produce a given amount of mechanical deterioration

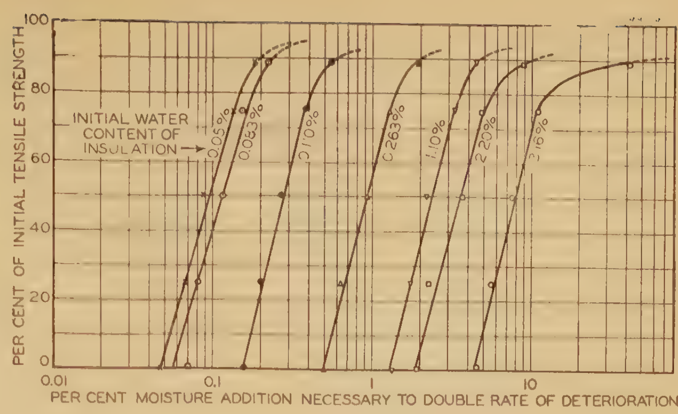


Figure 19. Illustrating the semilogarithmic relation which exists between the degree of mechanical deterioration of oil-treated and oil-immersed 0.003-inch Manila insulating paper and the amount of moisture addition necessary to double the rate of deterioration (halve the life) when heated in sealed glass vessels, the surface of the oil being in contact with nitrogen gas

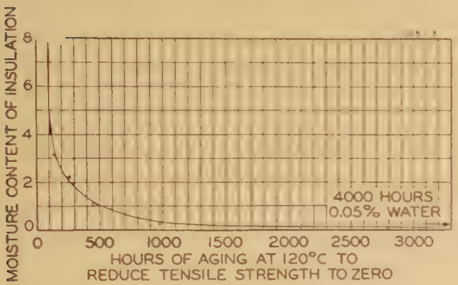


Figure 18. The elimination of moisture from cellulose insulation before being subjected to high temperature produces a rapidly increasing life as the moisture content is reduced below three per cent

Data based on the ultimate life values illustrated in Figure 16

life of the same insulation containing 3.16 per cent water from its value of 140 hours aged at 120 degrees centigrade, the addition of approximately 4.4 per cent more water is necessary. The relationship between the degree of mechanical deterioration of the oil-treated and oil-immersed 0.003-inch Manila insulating paper and the amount of moisture necessary to double the rate of deterioration (halve the life) in the absence of oxidation is shown in Figure 19.

Figure 19 is of importance. It demonstrates that as the point of mechanical strength selected for comparison approaches the original tensile strength of the insulation, the effect of water additions is reduced in the absence of oxygen, that is, greater water additions are necessary to reduce the life by half. This supports the observation that the "stable period," the length of time that the material can be heated without apparent mechanical change, is not greatly affected by changes in the water content of the insulation. Figure 19 also indicates that the effect of water additions becomes more pronounced as the degree of deterioration of the insulation progresses. This supports the observation that those reagents active in promoting mechanical deterioration include acidic products formed as a result of the initial chemical changes, the action of which is made more severe because of the presence of moisture.

Summary Conclusions

The general conclusions which are drawn from this investigation are:

1. The rate of mechanical deterioration in cellulose insulation depends on the conditions of its use. Among those factors of major importance are the temperature applied and the presence of oxygen and moisture.

2. As the temperature is raised above 75 degrees centigrade, the mechanical deterioration of cellulose insulation proceeds at a rapidly increasing rate.

3. Oxidation is most effective on the total mechanical life of the cellulose when the insulation is aged at temperatures below 120 degrees centigrade.

4. The effect of intermittent heating at 150 degrees centigrade on the mechanical deterioration of cellulose insulation is additive.

5. The mechanical life of a cellulose sheet is radically reduced when the water content of the dry insulation is increased in amount up to approximately 0.5 per cent of its dry weight. In general terms, the mechanical life of cellulose insulation is reduced by half for each 100 per cent increase in water content.

6. The effect of oxidation on the mechanical life of cellulose insulation becomes more pronounced in the presence of moisture.

7. The rate of deterioration for the substantially dry insulation as affected by temperature increase above 120 degrees centigrade is dependent upon its previous history. The "eight-degree rule," indicative that the rate of mechanical deterioration is doubled for each eight-degree-centigrade increase from a base temperature of 120 degrees centigrade or higher, applies most closely for practical use when the insulation under study has lost more than 50 per cent of its initial tensile strength.

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Steady-State Theory of the Amplidyne Generator

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Synopsis: The fundamental steady-state theory of the amplidyne generator is presented in this paper together with methods for calculating the characteristics of the generator from the machine constants. Experimental data obtained on a test machine are compared with calculated results to show the accuracy of the methods and to substantiate the theory. The effects of such factors as brush losses, commutation, and overcompensation and undercompensation on the operation of the generator are explained. Overcompensated and undercompensated machines are treated from the standpoint of both steady-state operation and electrical stability. Methods are given for the determination of the machine constants from open-circuit and short-circuit tests performed on the machine. This method offers the advantage that the machine constants so determined apply for actual conditions of operation and need not be modified to include the effects of such factors as brush and commutation losses, eddy currents, and hysteresis.

THE amplidyne generator which has found wide industrial application in various types of control apparatus is closely related from the standpoint of operating principle to the Rosenberg generator and the metadyne, differing principally in the manner in which it is used. The Rosenberg generator is a constant current source of d-c electrical energy; the metadyne in its usual form is a machine for converting constant potential d-c energy into constant current d-c energy, while the amplidyne generator is a dynamoelectric power amplifier.

One of the outstanding features of the Rosenberg generator is that the open-circuit output voltage varies with the square of the speed for constant field excitation.[2] Therefore, the polarity of the machine is independent of the direction of rotation. When the terminal voltage is maintained constant, by a storage battery, for instance, the output current is essentially independent of the speed for very wide limits of speed variation. Because of these particular properties, this generator in conjunction with storage batteries has been widely used for train lighting service.[1,3,4]

Although the Rosenberg machine was first described in 1905,[1] apparently little was known as to the variety of characteristics this type of machine would assume if other coils in addition to the primary field were used in providing excitation. An investigation of this nature, conducted by J. M. Pestarini in France and published in 1930,[5–7] led to the conception of the metadyne. As defined by Mr. Pestarini, the metadyne is a generalized d-c machine consisting of a d-c armature and commutator, any number of field poles each of which may be excited in any manner, and any number of brushes arranged to bear on the commutator in positions for which satisfactory commutation can be obtained. In accordance with this definition, all rotating d-c machines including the ordinary d-c generator are metadynes. In common usage, however, the term "metadyne" refers only to those several forms of d-c machine which convert constant potential energy into constant current energy. The "figure-eight-connected" metadyne was found suitable for electric locomotive control and was successfully applied to many of the electric railways in France[6] and later to electric traffic systems in England.[8,9] Modifications of this general scheme have been used in this country for control of the locomotives of modern streamlined trains.

Investigation by the General Electric Company of possible applications of different forms of the metadyne to the control of industrial apparatus led to the development of the amplidyne generator.[12–14] This machine, which falls into the general class of metadynes designated as "cross-connected," represents a generalization of the Rosenberg generator in which the field poles are excited by several coils supplied from different sources either internal or external to the machine.

The amplidyne generator is used principally as an element of control mechanisms and plays a part similar to that of the ordinary vacuum tube. It absorbs weak current or voltage signals, amplifies them, and delivers them to the next unit of the control mechanism at a greatly increased power level. This machine offers the principal advantage that it may be built at very reasonable cost to deliver large power outputs. On the other hand, it is limited in application because of its comparatively slow speed of response. The time lag between a change in the input signal and the corresponding change in the output is comparatively large, because the machine consists entirely of highly inductive circuits. Its chief field of application is in control mechanisms operating on d-c or very low-frequency a-c signals. Essentially, the maximum available output voltage varies inversely with the square of the frequency of the applied signal.

The amplidyne generator is applicable to both "open" and "closed-cycle" control. The greater field, however, is in closed-cycle-control mechanisms because of certain inherent differences between the two types, as well as the more general use

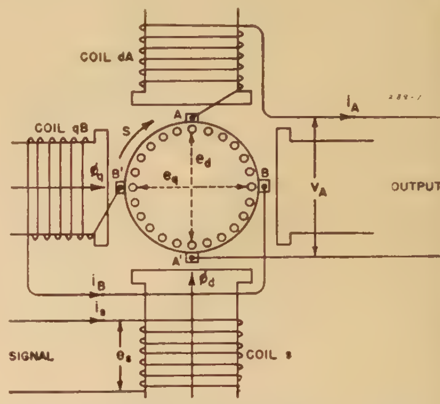

Figure 1. Essential elements and connections of the amplidyne generator

in recent years of closed-cycle control. "Open" control is that type of automatic control in which the quantity (time, voltage, displacement, and so forth) which actuates the control mechanism is essentially independent of the control operation. A simple example of this type is the automatic-starting compensator used for starting induction motors, synchronous motors, and so forth. These devices initially connect the machine terminals to a reduced voltage supply. By means of a time relay full line voltage is applied to the machine after normal speed is attained. This mechanism is a time-operated open-control device since it allows a definite time independent of acceleration for the starting period. "Closed-cycle" control is that type of automatic control in which the quantity which actuates the control mechanism is affected by the control operation. A good example of this

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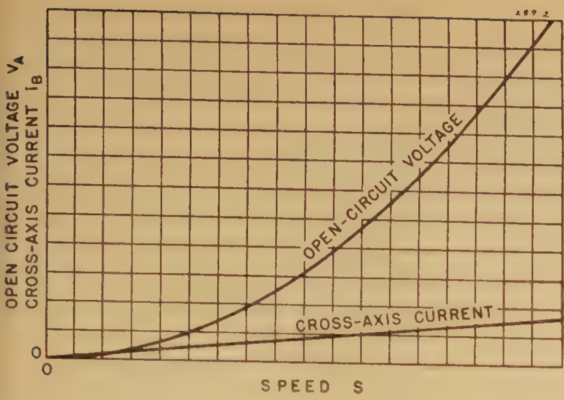
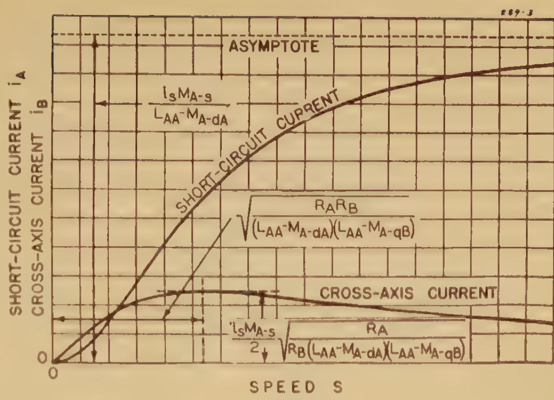


Figure 2 (left). Calculated curves illustrating the nature of the open-circuit characteristics of the amplidyne generator

Figure 3 (right). Calculated curves illustrating the nature of the short-circuit characteristics of an undercompensated amplidyne generator



type is automatic ship steering. Here, the angle between the ship compass and the hull is used to control the rudder, and this angle in turn is affected by the rudder operation.

In the first type of control the source of the controlling quantity usually has sufficient power to operate the control mechanism directly. In the second type the source of control usually is some form of measuring instrument which seldom can deliver any appreciable amount of power. Therefore, the original signal must be amplified in order that sufficient power be available to drive the control mechanism. The amplidyne generator provides a very satisfactory means of obtaining the necessary power amplification when a d-c control signal is used, and when a large amount of power is required to drive the control mechanism.

Closed-cycle-control mechanisms usually operate on the difference of two quantities, that is the difference between the desired quantity and the quantity which actually results from the output of the control mechanism. In the ideal case this difference would be zero, but this result is never obtained in practice. Comparison with the ideal, however, furnishes a standard by which the actual operation of the control mechanism may be judged.

In studies of the operation of a control mechanism the over-all behavior of the complete system including all main and control units must be analyzed, and the individual units so designed and adjusted that stable operation of the entire system is obtained. At the same time, the operation must be compared with that of the ideal in order that actual working tolerances may be kept to a satisfactory minimum. It is essential, therefore, that the operation of each individual unit, particularly in the control system, be expressible in simple quantitative terms. The load-voltage characteristic equation of the amplidyne generator (equation 14 of the appendix) defines the over-all operation of this machine and may be used in such analyses. It is valid for any type of load supplied by the generator, provided that the range of operation on the magnetization curve is sufficiently low that saturation effects are negligible. In most control applications this condition would be fulfilled, since a linear relationship between the signal and the output from the amplifying unit is usually desirable.

In instances where satisfactory operation is not attained for one reason or another, a comprehensive knowledge of the behavior of the various units comprising the control mechanism is necessary in order that proper adjustments or modifications may be made. Other characteristics of the amplidyne generator such as the variation of voltage and short-circuit current with speed and the effects of various coils on these characteristics provide this information. Overcompensation and undercompensation are necessary considerations, because these factors influence the stability of the machine. Generally speaking, overcompensation results in a higher output for the same signal input but causes inherent tendencies toward instable operation.

The essential electric elements of the amplidyne generator are an ordinary d-c armature and commutator, two complete sets of brushes spaced 90 electrical degrees apart, and windings connected as shown in Figure 1. The signal to be amplified, usually direct current, is applied to coil *s*. Current in this coil gives rise to a flux in the main axis of the machine, that is in the axis in line with the brushes *AA′*. Motion of the armature conductors in this flux generates a voltage in the cross axis, that is between the brushes *BB′*. Since these brushes are connected electrically, a current flows which produces a flux in the cross axis by virtue of armature reaction. The coil *qB* may be omitted in an actual machine but is included in the figure, since commutating poles used to secure good cross-axis commutation produce an effect on the machine constants similar to that of the coil *qB*. If present, this coil may be connected so as to add to or subtract from the magnetomotive force produced by the current flowing in the armature coils. The armature rotating in the cross-

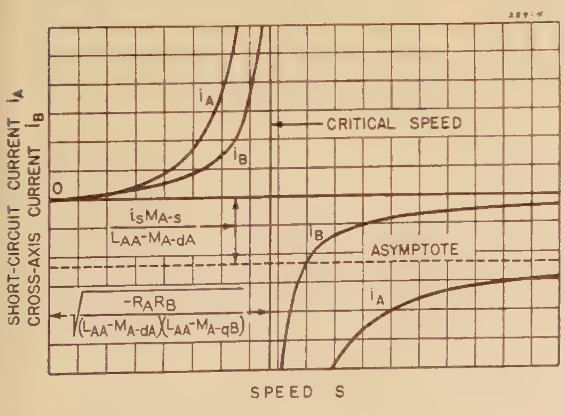
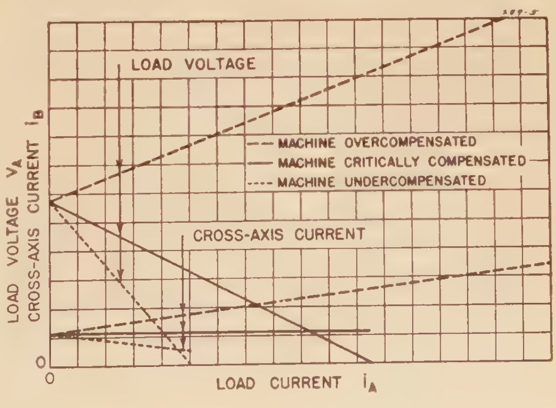


Figure 4 (left). Calculated curves illustrating the nature of the short-circuit characteristics of an overcompensated amplidyne generator

The machine is unstable even in the steady state for any speed above the critical speed

Figure 5. Calculated curves illustrating the nature of the load characteristics of the amplidyne generator and the effect of different degrees of main-axis compensation on these characteristics



axis flux gives rise to a voltage in the main axis which appears between the brushes AA'. This is the source of the output voltage of the machine. The purpose of the coil dA is to compensate for armature reaction in the main axis.

Open-Circuit Characteristics

The open-circuit or no-load characteristics of the amplidyne generator are expressed by equations 8, 9, and 10 of the appendix. The signal current is a function only of the signal voltage and the resistance of the signal coil. The cross-axis current varies linearly with the speed, and the output voltage varies with the square of the speed. The nature of these characteristics is illustrated by the calculated curves of Figure 2. The cross-axis current and the output voltage on open circuit are independent of the number of turns in the compensating coil dA.

The magnitude of the open-circuit voltage of the machine is governed by the mutual inductance between the signal coil and the armature and by the two cross-axis constants L_{AA} and M_{A-qB}. With other factors constant, doubling the number of turns in the armature coils increases the output voltage eight times if the coil qB is not present. In order to obtain a high output voltage, the cross-axis circuit resistance must be low as this factor appears in the denominator of the open-circuit voltage equation. The voltage acting in the cross axis is usually quite small, of the order of a few volts, so that brush drop represents a considerable portion of the total cross-axis circuit resistance. It is imperative, therefore, that material of very low contact resistance be used for the cross-axis brushes.

Short-Circuit Characteristics

The short-circuit characteristics of the amplidyne generator are expressed by equations 11, 12, and 13 of the appendix. The signal current depends only upon the signal voltage and the resistance of the signal coil. The nature of the two current characteristics depends upon the degree of compensation of the machine, that is upon the relative number of turns in the compensating coil dA. These characteristics are illustrated for an undercompensated machine by the calculated curves of Figure 3. The cross-axis current i_B has an initial slope the same as that of the open-circuit cross-axis current curve. The slope, however, decreases with increase in speed becoming zero when the condition $S^2(L_{AA}-M_{A-dA})(L_{AA}-M_{A-qB}) = R_AR_B$ is satisfied. As the speed increases further, the cross-axis current decreases and approaches zero asymptotically.

The short-circuit current i_A, beginning with an initial slope of zero, increases with the speed approaching asymptotically a definite finite value which depends upon the degree of compensation of the machine. As the ratio $R_AR_B/(L_{AA}-M_{A-qB})$ is made smaller by careful design of the machine, the short-circuit current approaches more closely the asymptotical value for any given maximum speed.

The asymptotical value of the short-circuit current, as obtained from the limit of equation 13 as S becomes infinite, is given by the expression $M_{A-s}i_s/(L_{AA}-M_{A-dA})$. With the compensating coil dA connected in the usual manner with regard to polarity, the value of the asymptote becomes larger and larger as turns are added to the compensating coil. When the number of turns are such that M_{A-dA} is equal to L_{AA}, the asymptote disappears, and the short-circuit current varies directly with the square of the speed of the machine. This condition may be used to define the boundary between overcompensation and undercompensation in the main axis. If the number of turns in the compensating coil are further increased so that M_{A-dA} is greater than L_{AA}, the value of the asymptote becomes negative, and the speed-current curve has a discontinuity at the point where $S^2(M_{A-dA}-L_{AA})\times(L_{AA}-M_{A-qB})=R_AR_B$. The operation of an overcompensated machine is stable in the steady state for speeds below the critical speed represented by this condition. At this critical speed the operation becomes unstable, and the short-circuit current increases without limit. For higher speeds, theoretically, the current reverses direction and decreases asymptotically to the value given by the asymptote expression. The curves for this condition illustrated in Figure 4 were calculated on the basis of the same constants as the curves of Figures 2 and 3 except that the value of the constant M_{A-dA} was increased approximately 20 per cent. This value represents a machine approximately six per cent overcompensated; that is, there are six per cent more turns in the compensating coil dA than those required for critical compensation as defined from the relation $L_{AA}=M_{A-dA}$.

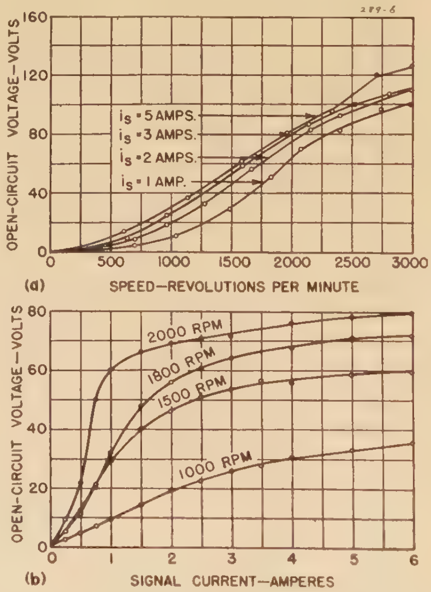


Figure 6. Experimental curves illustrating the effect of hard carbon brushes

For satisfactory operation the speed would have to be restricted to a maximum of 1,000 rpm and the signal current to 2.5 amperes. Brush drop resulting from higher speeds and signal currents causes excessive distortion as indicated by the crowding of the curves of (a) and the nonlinearity of the curves of (b). Compare with the curves of Figure 7 which were obtained from the same machine using graphite brushes

Load Characteristics

The load characteristics of the amplidyne generator are expressed by equations 14 and 15 of the appendix. For a given signal current and speed the curves are straight lines. If the machine is undercompensated, the lines are drooping, that is of negative slope, at any constant speed. If overcompensated, the lines are drooping at low speeds and are rising, that is of positive slope, at high speed. The load-voltage load-current characteristic becomes horizontal when the relationship $S^2(L_{AA}-M_{A-dA})(L_{AA}-M_{A-qB}) = -R_AR_B$ is satisfied. If the machine is critically compensated, the curve is drooping, but the slope is independent of the speed of the machine. The effect of different degrees of compensation on the load characteristics is illustrated by the curves of Figure 5, which were calculated for a three per cent overcompensated, a critically compensated, and a three per cent undercompensated machine respectively. For all three sets of curves, the speed is the same and is greater than the critical speed at which instability results for the overcompensated machine on short-circuit.

Low cross-axis circuit resistance is necessary for high output from the machine since the factor R_B appears in the denominators of all terms. If R_B is reduced to half its original value, both intercepts of the load-voltage load-current curve are doubled, and the maximum available power output is increased four times for the same signal current and

speed. The maximum available power output is equal to one-fourth the product of the two intercepts of the load-voltage load-current characteristic curve.

A critically compensated machine is stable in the steady state, regardless of the speed or the type of load. An overcompensated machine becomes unstable and builds up as a series generator for certain conditions, depending upon the speed of the machine, the degree of overcompensation, and the type of load supplied. If the machine supplies a constant impedance load, stable operation in the steady state occurs as long as the condition $(R_A+R_L)R_B > S^2(M_{A-dA}-L_{AA}) \times (L_{AA}-M_{A-qB})$ is fulfilled. The resistance of the load impedance is R_L. Thus, in the practical application of an overcompensated machine, instable operation may result if the load resistance is too low or if the speed of the machine is too high.

A critically compensated or overcompensated amplidyne generator may display a form of transient instability characterized by the output voltage, and also by the output current, building up to a very high value before coming to the final steady-state value when a sudden increase of load occurs. Transients of this character arise from the magnetic coupling between the compensating coil, the main-axis armature circuit, and the signal coil. In general, the compensating coil is more closely coupled with the signal coil than is the armature, because of the air gap. For critical compensation, the compensating coil must contain a few more turns than the equivalent number of turns on the armature. Thus, a sudden change of current in the load circuit will induce a current in the signal circuit of such direction as to increase the strength of the main-axis flux. This, in turn, causes a greater output voltage and greater output current from the machine. In some cases this action may be cumulative and result in free oscillation. In order to eliminate the possibility of transients of this character, the number of turns in the compensating coil dA must be reduced to the point where the transient components of current induced in the signal coil by sudden changes in the load current give rise to effects which oppose the original change of load current. This condition is fulfilled when M_{dA-s} is less than M_{A-s}.

To obtain the maximum output for a given maximum speed and signal current, the degree of compensation of the machine should be as high as possible. In order that the condition of critical compensation may be more closely approached without introducing tendencies toward instability, the compensating coil dA should be arranged with respect to the signal coil s so as to allow a lower coefficient of coupling between the two. This coefficient of coupling theoretically should be the same as that between the compensating coil and the armature.

Experimental Data and Results

In order to check experimentally the theory and equations presented in this paper, an amplidyne generator was constructed from a General Electric type *RI*, three-horsepower, induction motor. The armature of this machine was used without modification since it was wound with a four-pole wave winding. The stator was rewound with a four-pole distributed winding, part of the slots being used for an interpole winding in order that satisfactory commutation could be obtained at the cross-axis brushes. The compensating coil dA consisted of 14 turns per coil, four coils per pole, with taps brought out so that 9, 10, 11, 12, 13, or 14 turns per

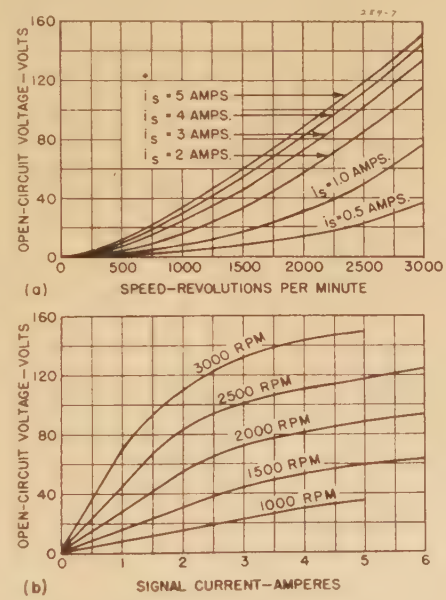


Figure 7. Experimental open-circuit characteristic curves of an amplidyne generator equipped with soft graphite brushes in the cross axis

coil could be used. The signal coil was wound with five turns per coil, four coils per pole. The interpole winding consisted of 50 turns surrounding each of the four stator teeth located in the cross-axis neutrals. While this design is not particularly efficient from the practical standpoint because of insufficient space for copper in the stator slots, it provides a satisfactory means for an experimental check of theory.

In making adjustments for satisfactory operation of the machine, it was found that the higher taps of the compensating winding gave so much overcompensation that they could not be used. The machine would build up as a series generator to a very high value of armature current when the machine was loaded. On the highest tap the machine would oscillate at a frequency of approximately 80 cycles per second for very low values of load resistance.

Physically, the amplidyne generator may be considered a two-stage amplifier. The first stage is represented by the voltage generated in the cross-axis circuit by the flux produced in the main axis by the signal coil. The second stage is represented by the voltage generated in the output circuit by virtue of the flux established in the cross-axis circuit by the cross-axis current. When load current flows in the output circuit a component of flux, in addition to that produced by the signal coil, is produced in the main axis. This component adds to the flux of the signal coil if the machine is overcompensated and subtracts if the machine is undercompensated. Thus, an overcompensated amplidyne generator is in effect a regenerative amplifier. It is unstable if too much of the output is fed back into the first stage of the machine by virtue of too many turns in the compensating coil. On the other hand, an undercompensated amplidyne generator is a degenerative amplifier and is quite stable. For this reason the tests were made with the compensating coil dA connected with nine turns per coil, giving a slightly undercompensated machine.

Hard carbon brushes were entirely unsatisfactory, as illustrated by the curves of Figure 6, because of the comparatively low voltages generated in the cross-axis circuit. Soft graphite brushes gave very satisfactory operation, but in this case the adjustment of the interpole shunt was quite critical. Best conditions of cross-axis commutation were obtained with a diverter which would by-pass approximately ten per cent of the total cross-axis current.

The characteristics of the test machine after the necessary adjustments were made to secure satisfactory operation are illustrated by the curves of Figures 7, 8, and 9. According to theory, the curves of Figure 7a should be parabolas, while the curves of the other five figures should be straight lines. In actual operation of the machine several factors such as magnetic saturation and brush drop cause the curves to deviate from the theoretical form.

The total ampere turns acting to produce flux in the space between brushes A and B and between brushes A' and B'

(see Figure 1) is proportional to the *sum* of the ampere turns acting in the cross axis and the ampere turns acting in the main axis. The total ampere turns acting to produce flux in the space between brushes B and A' and between brushes B' and A is proportional to the *difference* between the ampere turns acting in the cross axis and the ampere turns acting in the main axis. Thus, the total flux and the degree of magnetic saturation are greater in the air gap between brushes A and B and between brushes A' and B' than between brushes B and A' and between brushes B' and A. The total voltage generated in the cross-axis circuit is proportional to the total flux distributed around the armature between brushes B and B', and the total voltage generated in the output circuit is proportional to the total flux distributed around the armature between brushes A and A'. Since the same components of flux are involved in the generation of both of these voltages, the effects of magnetic saturation appear simultaneously in both the cross-axis and main-axis circuits.

In Figure 7b, the 1,000-rpm curve is essentially linear for signal currents up to four amperes, and the 2,000-rpm curve for signal currents up to two amperes. At higher signal currents magnetic saturation caused a decrease in the machine inductances with the result that the curves fall below the points represented by the initial linear relationship.

Since the measured resistance of the cross-axis circuit was slightly greater than one ohm, the change of the resistance of the graphite brushes with current was sufficient to cause a noticeable effect. This accounts for the upward turn of the 0.5-ampere and 1.0-ampere curves of Figure 8a near 2,500 rpm. For the 2-ampere curve the effects of magnetic saturation were appreciable for speeds above 2,200 rpm (as illustrated by the corresponding curves of Figure 8b). The slight decrease in the generated cross-axis voltage at high speed due to saturation was offset by the decrease in brush resistance. The resulting curve of cross-axis current was linear.

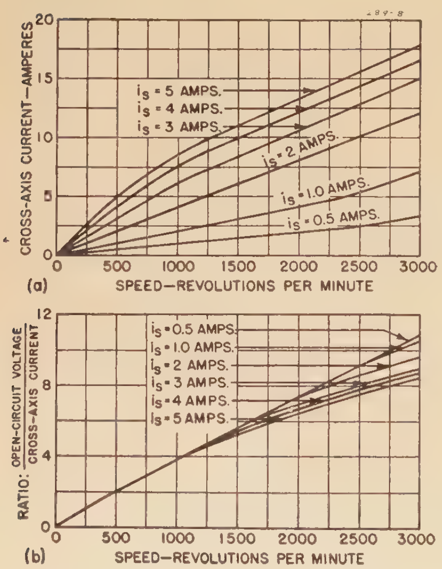


Figure 8. Open-circuit speed characteristics of the first and second stages respectively of an amplidyne generator

Nonlinearity of the curves of (b) are an indication of magnetic saturation of the machine

Characteristics of the Machine From Open-Circuit and Short-Circuit Tests

In order to calculate the operating characteristics of the amplidyne generator, machine constants must be used which are valid for actual conditions of operation. The values of the constants are influenced by magnetic saturation, by brush losses and commutation, and by hysteresis and eddy current losses. Therefore, any set of constants obtained from direct measurements of the impedances or inductances must be modified to include these factors. Such modifications are difficult to make accurately, since the effects of the losses on the machine constants cannot be expressed in simple quantitative terms. The necessity for making modifications can be eliminated to a large extent if the constants of the machine are calculated from test data from open-circuit and short-circuit tests performed on the machine.

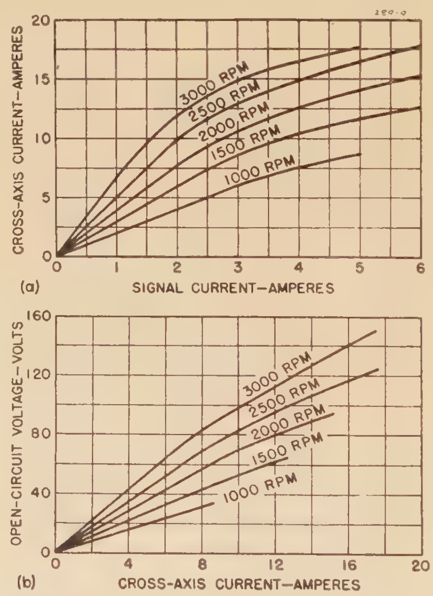


Figure 9. Open-circuit saturation curves of the first and second stages respectively of an amplidyne generator

Nonlinearity of the curves of (b) results from magnetic saturation of the machine, while nonlinearity of the curves of (a) result from magnetic saturation and brush drop at the cross-axis brushes

The two constants B and C are calculated from open-circuit test data in accordance with the following equations which result from equations 19 and 20 of the appendix:

$$\left.\begin{aligned} B &= i_B/Si_s \\ C &= V_A/Si_B \end{aligned}\right\} \text{Open-circuit test data used in these equations}$$

The constants A and R_A are calculated from short-circuit test data in accordance with the following equations which result from equations 21 and 22 of the appendix:

$$\left.\begin{aligned} R_A &= CSi_B/i_A \\ A &= (Bi_s/i_A) - (R_A/CS^2) \end{aligned}\right\} \text{Short-circuit test data used in these equations}$$

These four constants together with equations 19 to 23 are sufficient for the calculation of any of the steady-state characteristics of the amplidyne generator. The speed S in the equations above may be expressed in revolutions per minute instead of radians per second. In this case the speed S in equations 19 to 23 derived from these four constants must also be expressed in revolutions per minute.

The accuracy of this method for determining the characteristics of the amplidyne generator is shown by the curves of Figures 10 and 11, which were calculated on the basis of constants determined from open-circuit and short-circuit test data. For comparison actual test values are indicated by small circles and triangles plotted on the same axes.

In Figure 10a, the test points of the open-circuit voltage and cross-axis current curves for one-ampere signal current

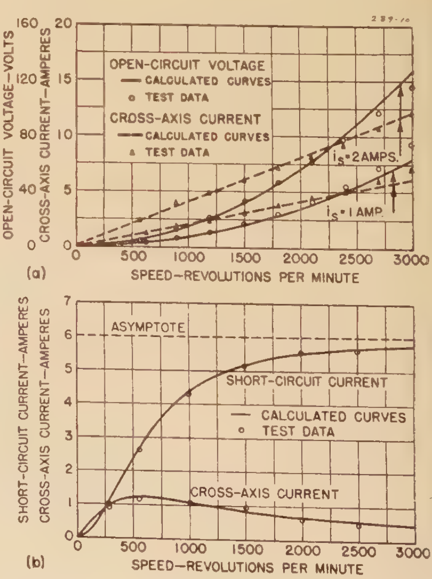


Figure 10. Comparison of calculated open-circuit and short-circuit characteristic curves with actual test data

fall above the calculated curves for speeds above 2,200 rpm. This discrepancy is due to the decrease in the resistance of the cross-axis brushes with increase in cross-axis current at the higher speeds. For the two-ampere signal current curve of cross-axis current, the change in brush resistance with current was offset by saturation, with the result that the test points agree very closely with the calculated curve. The test points of the corresponding open-circuit voltage curve fall below the calculated curve for speeds above 2,200 rpm because of saturation effects in the second stage of the machine.

Conclusions

Commutation and brush effects, particularly in the cross-axis circuit, have a very pronounced effect upon the operation of the amplidyne generator. Bad commutation and brush losses cause a considerable decrease in the available output. In order that the machine operate satisfactorily, the use of proper brush material and the realization of good commutation are imperative. To insure good commutation interpoles may be used to excellent advantage. The brushes in the cross-axis circuit must be of low resistance and produce as small a contact drop as possible.

The characteristics of the machine may be calculated on the basis of open-circuit inductances and resistances. The constants used for calculations must correspond with actual conditions of operation; but the main- and cross-axis coils may be treated as magnetically independent. The speed voltages appear in the opposite axes from the fluxes which produce them. The effects of magnetic saturation appear in both the main- and cross-axis circuits simultaneously. Saturation effects are difficult to include in calculations, because the degree of saturation changes with the speed and with the load.

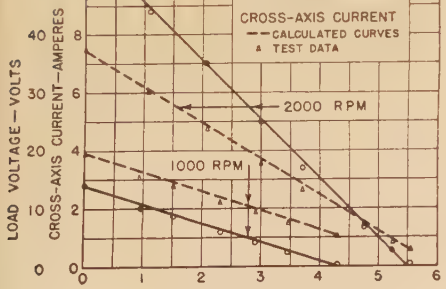


Figure 11. Comparison of calculated load characteristic curves with actual test data

Constants for the calculation of the steady-state characteristics of the amplidyne generator may be determined from open-circuit and short-circuit tests performed on the machine. This method offers the advantage that the values so obtained apply for actual conditions of operation. For purposes of design the characteristics may be predicted on the basis of the inductance coefficients, which may be obtained by the methods used for the more standard electrical machines by calculation of the various flux linkages involved. This method, however, is subject to considerable error because of the uncertainty of many factors, principally those relating to commutation, brush drop, and the associated losses.

The output load-voltage characteristic of an undercompensated machine is drooping (of negative slope) for all conditions of operation. The corresponding characteristic of an overcompensated machine may be either drooping or rising (of positive slope), depending upon the speed and the degree of overcompensation. Low speed together with a low degree of overcompensation gives a drooping characteristic which becomes horizontal and then rising if either of these two factors is sufficiently increased. A machine having a rising characteristic is unstable, even for steady-state operation, if the load resistance is less than a definite critical value which depends upon the slope of the characteristic curve.

Transient instability may result in an overcompensated or in an undercompensated machine, if the mutual inductance between the compensating coil and the signal coil is greater than the mutual inductance between the signal coil and the armature. Transient instability arising from this cause is characterized by the load current and the load voltage building up to very high values before coming to their final steady-state values when a sudden increase of load occurs. With a sudden decrease of load, the opposite effect takes place. In some instances this action may be cumulative and may result in free oscillation of the machine. If the compensating coil is arranged so that a fairly high leakage flux exists between it and the signal coil, a greater output is available from the machine without loss of stability.

The open-circuit voltage varies directly with the square of the speed, while the short-circuit current approaches asymptotically a definite value which depends upon the signal current and the design of the machine. For maximum power output and power amplification the speed should be as high as possible.

The available power output and also the power amplification become infinite for d-c operation of the machine at constant speed as the resistance of the cross-axis circuit approaches zero. As the resistance of the signal coil approaches zero, the power supplied by the signal source approaches zero, and the power amplification again becomes infinite. Since both of these resistances may be made quite low, the power amplification of an amplidyne generator operating on direct current is very high. The machine used in obtaining data for this paper gave a maximum value of approximately 300. This corresponds with an ordinary d-c generator having a field loss of one third of one per cent. By use of a specially designed stator core which would allow more copper in the signal coil and interpole windings, a power amplification of at least ten times this value could easily be obtained.

Appendix. D-C Operation of the Amplidyne Generator

If a d-c signal is applied to coil s (Figure 1), the steady-state behavior of the machine is expressed by the following equations:

$$e_s = i_sR_s \tag{1}$$

$$e_q = i_BR_B = KS\phi_d10^{-8} \tag{2}$$

$$e_d = i_AR_A + V_A = KS\phi_q10^{-8} \tag{3}$$

If the speed of the machine is measured in electrical radians per second, the constant K is the equivalent number of series armature turns between opposite brushes, that is the number of series turns which would give the same number of flux linkages if each of these turns linked all the flux in either axis. The expressions $K\phi_d$ and $K\phi_q$ are the total number of armature flux linkages in the main and cross axes of the machine respectively. The total flux which is effective in producing speed voltage in the output circuit, that is ϕ_d, includes the mutual flux from coils dA and s as well as the leakage flux of the armature. Likewise, the total flux which is effective in producing speed voltage in the cross-axis circuit includes the mutual flux from coil qB as well as the leakage flux of the armature. In terms of open-circuit self- and mutual inductances the flux linkages in the main and cross axes are:

$$K\phi_d = N_A\phi_d = (M_{A-s}i_s - L_{AA}i_A + M_{A-dA}i_A)10^8 \tag{4}$$

$$K\phi_q = N_A\phi_q = (L_{AA}i_B - M_{A-qB}i_B)10^8 \tag{5}$$

The negative sign is used before the mutual inductance M_{A-qB}, because the coil qB is assumed connected so as to compensate partially for armature reaction in the cross axis. This sign should be positive if the coil is connected the other way. Making use of these relations, equations 2 and 3 may be written:

$$i_sSM_{A-s} - i_BR_B - i_AS(L_{AA} - M_{A-dA}) = 0 \tag{6}$$

$$i_B S(L_{AA}-M_{A-qB})-i_A R_A=V_A \quad (7)$$

The open-circuit characteristics of the machine are obtained from the solution of these equations with the load current i_A equated to zero:

$$i_s=e_s/R_s \quad (8)$$

$$i_B=i_s S M_{A-s}/R_B \quad (9)$$

$$V_A=i_s S^2 M_{A-s}(L_{AA}-M_{A-qB})/R_B \quad (10)$$

The short-circuit characteristics of the machine are obtained from equations 1, 6, and 7 with the load voltage V_A equated to zero:

$$i_s=e_s/R_s \quad (11)$$

$$i_B=\frac{S M_{A-s} R_A}{S^2(L_{AA}-M_{A-dA})(L_{AA}-M_{A-qB})+R_A R_B} i_s \quad (12)$$

$$i_A=\frac{S^2 M_{A-s}(L_{AA}-M_{A-qB})}{S^2(L_{AA}-M_{A-dA})(L_{AA}-M_{A-qB})+R_A R_B} i_s \quad (13)$$

If the machine is supplying a constant resistance load, the voltage V_A in equation 7 is replaced by $i_A R_L$ in which R_L is the resistance of the load. The solutions for the three currents are exactly the same as the short-circuit characteristics except that the resistance of the main-axis circuit becomes (R_A+R_L) instead of R_A. The load voltage is found from the relation $V_A=i_A R_L$ after the currents are obtained.

The load characteristics of the machine are obtained from equations 6 and 7 by elimination of i_B and V_A respectively:

$$V_A=\frac{-[S^2(L_{AA}-M_{A-dA})(L_{AA}-M_{A-qB})+R_A R_B]}{R_B} i_A+\frac{S^2 M_{A-s}(L_{AA}-M_{A-qB}) i_s}{R_B} \quad (14)$$

$$i_B=\frac{-S(L_{AA}-M_{A-dA})}{R_B} i_A+\frac{S M_{A-s} i_s}{R_B} \quad (15)$$

These latter two equations are valid regardless of the type of load supplied by the machine. Equation 14 expresses the relation between the three variables V_A, i_A, and i_s, and may be used to define the overall operation of the amplidyne generator in any control circuit in which it is connected, provided the machine operates low enough on the magnetization curve so that saturation effects may be neglected.

The equations given above may be simplified by the following substitutions:

$$A=(L_{AA}-M_{A-dA})/R_B \quad (16)$$

$$B=M_{A-s}/R_B \quad (17)$$

$$C=(L_{AA}-M_{A-qB}) \quad (18)$$

In terms of these constants the characteristic equations are:

Open-circuit characteristics:

$$i_B=BSi_s \quad (19)$$

$$V_A=BCS^2 i_s \quad (20)$$

Short-circuit characteristics:

$$i_B=\frac{BR_A S}{ACS^2+R_A} i_s \quad (21)$$

$$i_A=\frac{BCS^2}{ACS^2+R_A} i_s \quad (22)$$

Load characteristics:

$$V_A=-(ACS^2+R_A)i_A+BCS^2 i_s \quad (23)$$

$$i_B=-ASi_A+BSi_s \quad (24)$$

List of Symbols

e_d—Generated voltage in the main-axis or output circuit

e_q—Generated voltage in the cross-axis circuit

e_s—Voltage applied to the signal coil

L_{AA}—The self-inductance of the armature between either the main-axis brushes or between the cross-axis brushes

M_{A-dA}—The mutual inductance between the armature and the compensating coil dA

M_{A-qB}—The mutual inductance between the armature and the cross-axis coil qB

M_{A-s}—The mutual inductance between the armature and the signal coil s

M_{dA-s}—The mutual inductance between the compensating coil dA and the signal coil s

R_A—Resistance of the main-axis circuit internal to the machine. Includes the resistance of the armature, the main-axis brushes, and the compensating coil dA

R_B—Resistance of the cross-axis circuit. Includes the resistance of the armature, the cross-axis brushes, and the cross-axis coil qB

R_L—Resistance of the load impedance

R_s—Resistance of the signal coil s

S—Speed in electrical radians per second

V_A—Output or load voltage

ϕ_d—The total flux linking the armature in the main axis

ϕ_q—The total flux linking the armature in the cross axis

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Analysis of Short Circuits for Distribution Systems

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Synopsis: This paper presents a discussion and analysis of short circuits for low-voltage distribution systems. Many distribution systems are supplied from transformers connected in delta on the low-voltage side with the mid-tap of one secondary winding, or with one corner of the delta, connected to ground. As a result, short circuits on these systems often involve failure to ground, and an accurate analysis for these faults is of value. An original analysis of power-leg-to-ground and light-leg-to-ground short circuits is presented together with formulas for line-to-line and three-phase faults. A study of short circuits on the secondary circuits of single-phase transformers supplied from three-phase systems is also included, thereby giving a comprehensive treatment of distribution short circuits. The paper also includes a discussion of the effects of resistance in limiting short-circuit currents and the voltage rise of the secondary neutral above ground potential caused by transformer failure and resistance to ground.

DISTRIBUTION transformer banks supplying 240-volt three-phase and 120/240-volt single-phase loads generally have the mid-point of one secondary winding connected to ground. This is done to protect the low-voltage distribution systems against high voltage in the event of transformer failure and to provide a ground for the low-voltage systems. An accurate analysis of short circuits and related problems on these systems is important in the selection and setting of protective equipment and in reduction of personal and fire hazards. The ratings of these transformer banks are from about 6 to 1,000 kva. The most common short circuit in conduits and machines is failure to ground. In addition to the unsymmetrical conditions imposed by a fault to ground, special considerations are required, since it is assumed that the transformer secondaries are connected in delta and the ground return is furnished by a connection to the mid-point of one secondary winding. Other connections are in common use. Three-phase four-wire circuits may be supplied from transformers with the low-voltage secondary windings wye-connected with the neutral grounded, and transformers without mid-taps supplying three-phase three-wire 240 volts often have one corner of the secondary delta connected to ground. This paper presents an analysis of power-leg-to-ground and light-leg-to-ground instantaneous symmetrical short-circuit

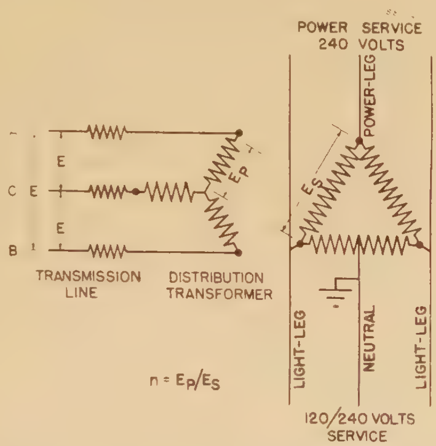

Figure 1. Typical distribution transformer with mid-point of one secondary winding grounded

currents and a discussion of short-circuit currents on distribution systems resulting from high-voltage transformer failures. Consideration of three-phase and line-to-line low-voltage short circuits and low-voltage short circuits on single-phase distribution transformers is also included.

The schematic diagram of Figure 1 gives the terminology and connections in common use for obtaining 240-volt three-phase three-wire and 120/240-volt single-phase three-wire services from a single transformer bank. For cases where the power load predominates, such as in industrial plants, the three transformers are ordinarily of the same capacity. Where single-phase loads predominate, for example, in rural installations, the two transformers used to furnish the power-leg are usually the same size, but they may be smaller than the third unit. In this study it is assumed that the three transformers are identical. However, in solving practical problems differences in capacity may be accurately taken into account for power-leg-to-ground short circuits, and approximations may be introduced for light-leg-to-ground faults.

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Effect of Resistance in Limiting Low-Voltage Short-Circuit Currents

Although resistance may produce a negligible effect in limiting short-circuit currents on high-voltage systems, it may have a considerable effect when faults occur on low-voltage circuits.[1] It is evident that the higher the voltage, the higher are the ohmic values of equipment impedance. Therefore, impedances having about the same ohmic values regardless of voltage rating may produce a negligible effect on high-voltage short-circuit currents when in combination with high-impedance equipment, but they may be very important on low-voltage circuits when connected in series with other low-impedance apparatus.[2] For 120- or 240-volt circuits resistance is often the predominating current-limiting factor, and omission of it (in the interest of simplicity) may lead to serious error. The importance of resistance is illustrated in Figure 2, wherein line-to-ground fault current* is plotted against ground or fault resistance for a few selected voltages. Resistance is included in both analysis and examples of the present discussion.

Power-Leg-to-Ground Short-Circuit Analysis

Transformers Connected in Wye-Delta

It is apparent from inspection of Figure 3, that a power-leg-to-ground short circuit is a special type of single-phase fault. The voltage effective in producing the fault current is $\sqrt{3}E_s/2$. The fault current is equal to the sum of two equal low-voltage secondary currents which are inphase with each other. Neglecting the effects of load and transformer exciting current, the instantaneous symmetrical secondary current is given by:

$$I_s = \frac{\sqrt{3}E_s/2}{z_{TL}+z_{(S-P)}+0.5z_{(S/2-S/2)}+2z}$$

in which

E_s = the voltage of the secondary winding on open circuit, in volts

* Refer to standard references for analysis of ordinary unsymmetrical short circuits (for example, reference 3 or 4).

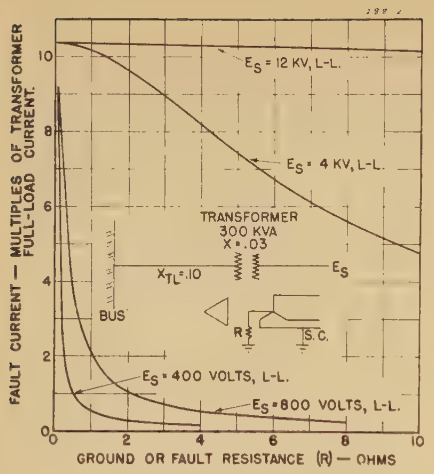


Figure 2. Effect of resistance in limiting short-circuit currents

z_{TL} = the impedance of the transmission line and other series equipment in ohms referred to the secondary voltage

$z_{(S-P)}$ = the impedance of the low-voltage secondary winding to the high-voltage primary winding in ohms (see Figure 3C)

$z_{(S/2-S/2)}$ = the impedance of one-half the low-voltage secondary winding to the other one-half low-voltage secondary winding in ohms (see Figure 3C)

z = The impedance of the external low-voltage circuit to fault, including arc, conductor, and conduit impedances, the resistance to ground, and the impedance of the ground return circuit in ohms

The fault current is:

$$I_F = 2I_s = \frac{\sqrt{3}E_s}{z_{TL}+z_{(S-P)}+0.5z_{(S/2-S/2)}+2z} \quad (1)$$

Let

E = the transmission-system voltage line-to-line, in volts

$n = E_p/E_s = E/\sqrt{3}E_s$, the transformer turn ratio

Then

$E_s = E/n\sqrt{3}$

and

$$I_F = \frac{E/n}{[Z_{TL}+Z_{(P-S)}]/n^2+0.5z_{(S/2-S/2)}+2z} \quad (2)$$

where

Z_{TL} = the impedance of the transmission system in high-voltage ohms. This assumes that the reactance of one wire to neutral is the same for both single-phase and three-phase balanced currents. This is true for circuits with equilateral spacing or completely transposed circuits. It is unlikely that the distribution feeder would be either equilaterally spaced or completely transposed between the source and a particular transformer installation, and for greatest accuracy the actual single-phase reactance between the two high-voltage primary line conductors carrying current should be used

$Z_{(P-S)}$ = the impedance of the high-voltage primary winding to the low-voltage secondary winding in high-voltage ohms. See Figure 3C.

The primary line currents are obtained by inspection of Figure 3B:

$$I_A = I_B = I_s/n = I_F/2n, \text{ and } I_C = 0 \quad (3)$$

Transformers Connected in Delta-Delta

The analysis for the case in which the transformers are connected in delta-delta is similar to that of the preceding derivation with the exception of the modifications required due to the high-voltage delta connection. See Figure 4.

$$I_F = 2I_s = \frac{\sqrt{3}E_s}{3z_{TL}+z_{(S-P)}+0.5z_{(S/2-S/2)}+2z} \quad (4)$$

For this case $n = E_p/E_s = E/E_s$. In terms of the transmission voltage and actual transmission impedances in high-voltage ohms, the fault current is given by

$$I_F = \frac{\sqrt{3}E/n}{[3Z_{TL}+Z_{(P-S)}]/n^2+0.5z_{(S/2-S/2)}+2z} \quad (5)$$

From inspection of Figure 4B

$$I_B = I_C = I_s/n = I_F/2n, \text{ and } I_A = 2I_B = I_F/n \quad (6)$$

Light-Leg-to-Ground Short-Circuit Analysis

Transformers Connected in Wye-Delta

The light-leg-to-ground short-circuit analysis is developed from the method of symmetrical components as follows: The schematic diagrams of Figure 5 are based on the assumption that the system is entirely symmetrical. The dissymetry imposed by the terminal conditions which represent this type of fault is inserted in the analysis at the appropriate point.

Let

$n = E_p/E_s = E/\sqrt{3}E_s$, the transformer turn ratio

z_{TL}' = the impedance of the transmission line and other series equipment in ohms referred to one-half the secondary voltage, that is,

$$z_{TL}' = Z_{TL}\Big/\left[\frac{E_p}{E_s/2}\right]^2 = Z_{TL}/(2n)^2 \quad \text{where}$$

Z_{TL} = the impedance of the transmission line and other series equipment in high-voltage ohms

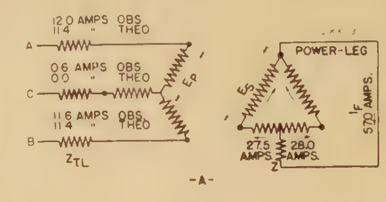
$z_{(S/2-P)}$ = the impedance of one-half the low-voltage winding to the high-voltage primary winding in ohms, and

$z_{(S/2-P)} = Z_{(P-S/2)}/(2n)^2$*

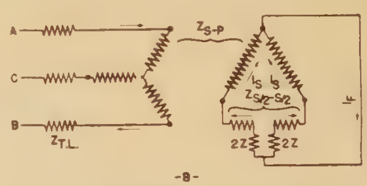
$z_{(S/2-S)}$ = the impedance of one-half the low-voltage winding to the entire low-voltage winding in ohms (see Figure 5D)

z = the impedance of the external low-voltage circuit to fault, including arc, conductor, and conduit impedances, the resistance to ground, and the impedance of the ground return circuit, in ohms

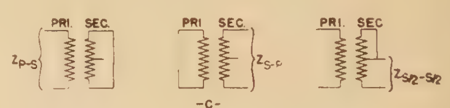
The positive- and negative-sequence impedances to fault are obtained from inspection of Figure 5B. All impedances are actual ohms, or expressed in ohms referred to one-half the secondary voltage on open circuit. Z_{2F} is taken equal to Z_{1F} for determination of instantaneous symmetrical short-circuit currents. The proper generator impedances may be included in the term Z_{TL} when transient or sustained currents are desired.



A. Schematic diagram with experimental values inserted



B. Equivalent circuit for analysis



C. Determination of transformer impedances

Figure 3 Power-leg-to-ground short-circuit analysis

Transformers connected in wye-delta

The positive-sequence impedance to fault is

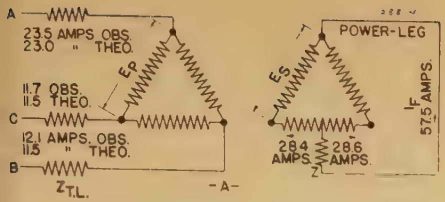
$Z_{1F} = z_{TL}' + z_{(S/2-P)} + z$

The zero-sequence impedance to fault is obtained from Figure 5C.:

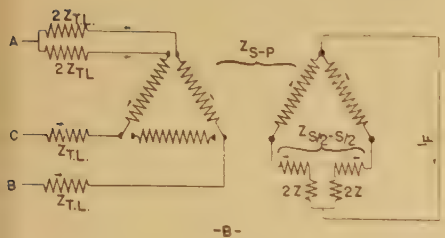
$Z_{0F} = z_{(S/2-S)} + z$

The equations for a line-to-ground

* The first subscript denotes the winding upon which the impedance measurements are made, or the side to which they are referred. The second subscript refers to the winding short-circuited for the impedance test. See Figure 5D.



A. Schematic diagram with experimental values inserted



B. Equivalent circuit for analysis

Figure 4. Power-leg-to-ground short-circuit analysis.

Transformers connected in delta-delta

short circuit are (see reference 3 or 4 for details):

$$I_1=I_2=I_0=I_F/3, \text{ and } I_F=\frac{3E}{Z_{1F}+Z_{2F}+Z_{0F}}$$

The voltage effective in producing fault current is $E_s/2$. Substituting the above expressions for the impedance to fault and the voltage effective in producing the fault current gives

$$I_F=\frac{3E_s/2}{2[z_{TL}'+z_{(S/2-P)}]+z_{(S/2-S)}+3z} \quad (7)$$

In terms of the transmission voltage and actual transmission impedances in high-voltage ohms, the above becomes

$$I_F=\frac{\sqrt{3}E/2n}{2[Z_{TL}+Z_{(P-S/2)}]/(2n)^2+z_{(S/2-S)}+3z} \quad (8)$$

The primary currents are obtained from inspection of Figure 6:

$$I_C=(I_1+I_2)/2n=2/3(I_F/2n)=I_F/3n, \text{ and } I_A=I_B=I_F/6n \quad (9)$$

Transformers Connected in Delta-Delta

The analysis for the case in which the transformers are connected in delta-delta is similar to the foregoing with exceptions which are apparent from inspection of Figures 7 and 8.

$$Z_{1F}=Z_{2F}=z_{TL}'+z_{(S/2-P)}+z$$

For this case

$$z_{TL}'=Z_{TL}\Big/\left[\frac{E_P/\sqrt{3}}{E_s/2}\right]^2=3Z_{TL}/(2n)^2$$

$$Z_{0F}=z_{(S/2-S-P)}+z$$

in which

$z_{(S/2-S-P)}$ = the impedance of one-half the low-voltage secondary winding to the entire low-voltage winding and high-voltage winding in ohms. See Figure 7D.

Following the procedure of the previous derivation:

$$I_F=\frac{3E_s/2}{2[z_{TL}'+z_{(S/2-P)}]+z_{(S/2-S-P)}+3z} \quad (10)$$

Or, in terms of the transmission voltage and actual transmission impedances in high-voltage ohms

$$I_F=\frac{3E/2n}{2[3Z_{TL}+Z_{(P-S/2)}]/(2n)^2+z_{(S/2-S-P)}+3z} \quad (11)$$

The primary line currents are obtained from inspection of Figure 8. The zero-sequence currents circulate in both delta-connected windings, and the relative division could be worked out by using the methods developed for analyzing three-winding transformers. However, since the zero-sequence currents cannot pass from the delta into the high-voltage lines, this refinement is not necessary, and the line currents are determined directly from the positive- and negative-sequence currents as indicated in the figure.

$$I_B=I_C=I_F/2n, \text{ and } I_A=0 \quad (12)$$

Three-Phase Short-Circuit Analysis

Transformers Connected in Delta on Low-Voltage Side

Although three-phase short circuits are of infrequent occurrence on distribution circuits, they occasionally occur on open bus structures or in other equivalent locations. The fundamental equation for three-phase short-circuit current is

$$I_{3\phi}=\frac{E_s/\sqrt{3}}{z_{TL}''+z_{(S-P)}'+z'} \quad (13)$$

in which

z_{TL}'' = the impedance of the transmission line and other series equipment in ohms referred to the secondary voltage to neutral. For transformers connected in wye-delta

$$z_{TL}''=Z_{TL}\Big/\left[\frac{E_P}{E_s/\sqrt{3}}\right]^2=Z_{TL}/3n^2$$

For transformers connected in delta-delta

$$z_{TL}''=Z_{TL}\Big/\left[\frac{E_P/\sqrt{3}}{E_s/\sqrt{3}}\right]^2=Z_{TL}/n^2$$

$z_{(S-P)}'=z_{(S-P)}/3$, the equivalent wye, or line-to-neutral impedance of the transformer bank, in low-voltage ohms.

z' = the line-to-neutral impedance of the external circuit to fault in low-voltage ohms including conductor impedance and fault impedance, that is, the impedance of one of three assumed equal arcs to their common meeting point.

Line-to-Line Short-Circuit Analysis

Transformers Connected in Delta on Low-Voltage Side

Line-to-line short circuits frequently occur on exposed wires and in open bus structures. This type of fault is probably most frequent on distribution systems where one corner of the secondary delta is connected to ground. Analysis is made by using the method of symmetrical components.[3,4] In terms of the open-circuit secondary voltage, the low-voltage fault current is

$$I_{L-L}=\frac{E_s}{2[z_{TL}''+z_{(S-P)}']+z} \quad (14)$$

in which the quantities have been previously defined. For this case z equals the total external impedance of the external circuit to fault in low-voltage ohms (in contrast to z' used for the three-phase analysis).

Short Circuits on Single-Phase Transformer Secondary Circuits

Transformers Supplied From Two High-Voltage Lines

The problem of determining short-circuit currents on single-phase transformer secondary circuits is so important that it deserves mention. If the transformer and total external impedance to fault is referred to the high-voltage side, the equation is obtained from elementary methods by inspection:

$$I_{HT1\phi}=\frac{E}{2Z_{TL}+Z_{(P-S)}+Z} \quad (15)$$

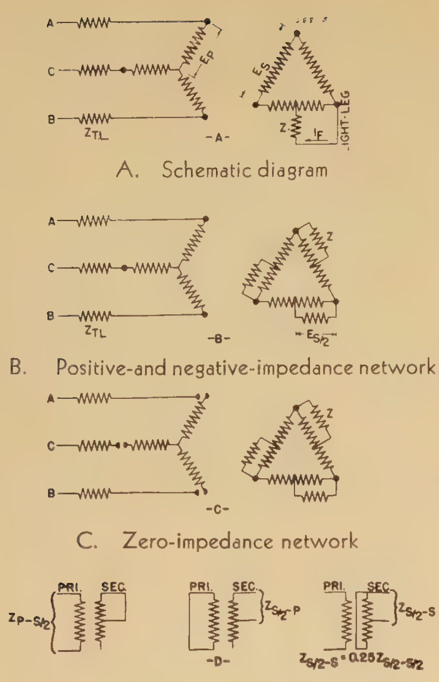
in which

$Z=n^2z$, the impedance of the external circuit to fault in equivalent high-voltage ohms

Or, the low-voltage secondary current equals:

$$I_F=\frac{E_s}{2Z_{TL}/n^2+z_{(S-P)}+z} \quad (16)$$

Equations 15 and 16 give correct results for symmetrical subtransient fault currents. The line-to-line fault analysis developed by using the method of symmetrical components is a better approach from the strictly technical viewpoint. This gives the same equations but has the advantage that the proper generator constants may be included in the transmission-system impedance term and



A. Schematic diagram

B. Positive-and negative-impedance network

C. Zero-impedance network

D. Determination of transformer impedances

Figure 5. Light-leg-to-ground short-circuit analysis

Transformers connected in wye-delta

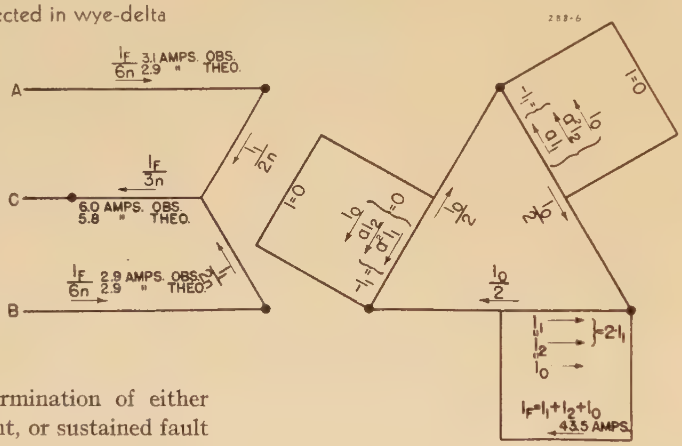


Figure 6. Detailed current distribution for a light-leg-to-ground short circuit

Transformers connected in wye-delta. Experimental values inserted

thereby permit determination of either subtransient, transient, or sustained fault current.

Transformers Supplied From One High-Voltage Line and Neutral

Analysis for this case is similar to the preceding analysis, and from the method of symmetrical components

$$I_{HT1\phi}=\frac{3E/\sqrt{3}}{2Z_{TL}+Z_{0TL}+3Z_{(P-S)}+3Z} \tag{17}$$

or the low-voltage secondary current equals

$$I_F=\frac{3E_s}{(2Z_{TL}+Z_{0TL})/n^2+3z_{(S-P)}+3z} \tag{18}$$

in which Z_{0TL}= the zero-sequence impedance of the transmission system in high-voltage ohms.

The equations for line-to-ground faults on three-wire systems supplied from single-phase transformers having the mid-tap of the low-voltage secondary winding connected to ground are the same as the above with the following modifications: The voltage effective in producing short-circuit current is now $E_s/2$, the ratio of transformation is $2n$, and the transformer impedance is $z_{(S/2-P)}$.

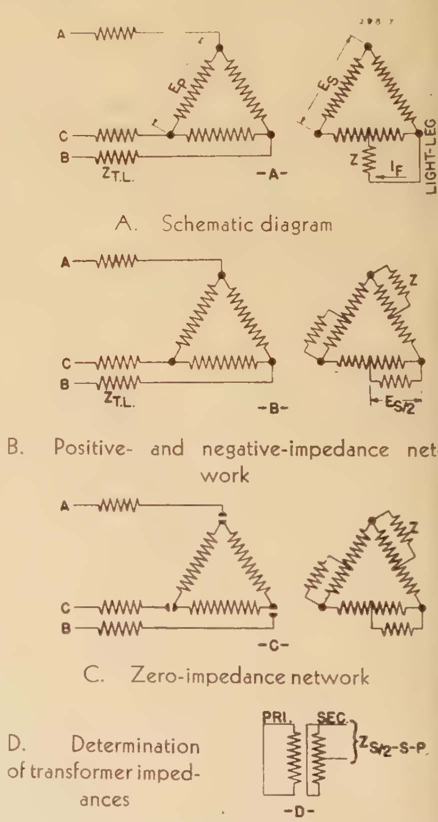
Short-Circuit Currents on Distribution Systems Resulting From High-Voltage Transformer Failures

During the initial stages of an internal failure between the high-voltage and low-voltage windings of a transformer, that is, before any of the conductors have been opened due to arcing, the high-voltage fault current flowing through the low-voltage secondary windings induces compensating currents in the high-voltage windings which circulate back through the high-voltage circuits. Also, under these conditions the transformer saturates because of high-voltage impressed on the low-voltage windings. Saturation neglected the requirement of equal and opposing ampere turns is met, and the current limiting effect of the transformer is due to leakage impedance. Actually, because of saturation, the effective impedance may be somewhat less than the leakage impedance. For purposes of illustration consider a three-phase distribution transformer supplied from a transmission line of reasonable length and assume that short circuits occur between each high-voltage terminal and the corresponding low-voltage terminal. The impedance limiting the fault current is equal to the transmission-system impedance up to the transformer terminals in high-voltage ohms, plus the impedance of the transformer in low-voltage ohms. Thus, the additional impedance opposing the high-voltage short-circuit current flowing in the secondary winding equals the transformer impedance in high-voltage ohms divided by the ratio of transformation squared (that is, n^2).

For most practical distribution systems n^2 is large, and the additional current-limiting effect due to the transformer is small. For 2,400 delta 240-volt transformers $n^2=100$, and the additional impedance is one per cent of the transformer leakage impedance (saturation neglected).

Where the windings have been opened, and compensating currents cannot flow, the current-limiting effect of the transformer is due to exciting impedance. Very high saturation results, and it has been suggested that the exciting impedance may approach leakage reactance in magnitude. Analysis for this case is similar to that in the preceding paragraph. The saturated exciting impedance in high-voltage ohms is reduced by the square of the turn ratio, and the effect of the transformer may be neglected as a first approximation.

For unsymmetrical transformer failures, such as a breakdown near the end turns or between the high- and low-voltage bushings, the effect of transformer impedance is likewise small. The voltage effective in producing the fault current equals the vector sum of the voltage of the high-voltage system and the induced voltage of the secondary winding. In cases where the ratio of transformation is small, this effect may



A. Schematic diagram

B. Positive- and negative-impedance network

C. Zero-impedance network

D. Determination of transformer impedances

Figure 7. Light-leg-to-ground short-circuit analysis

Transformers connected in delta-delta

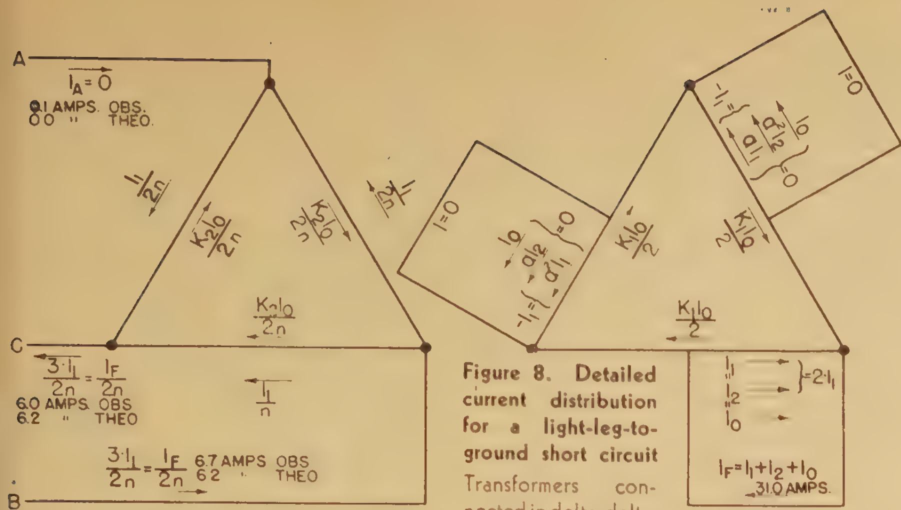


Figure 8. Detailed current distribution for a light-leg-to-ground short circuit

Transformers connected in delta-delta. Experimental values inserted

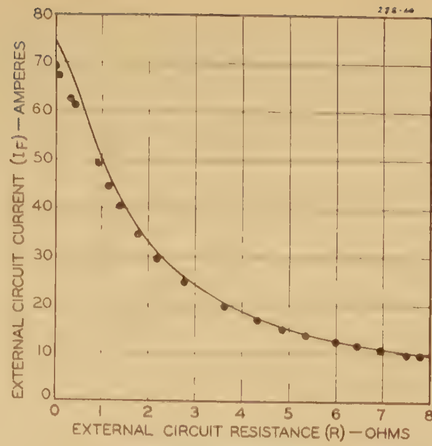


Figure 10. Power-leg-to-neutral experimental short circuits

Transformers connected in delta-delta

be important. However, for cases where the ratio is 10/1 or greater, this effect is also small and may be neglected for approximate work. An internal failure between the windings introduces additional complications, but it is unlikely that the fault current would be greater than that caused by breakdown near the extremities of the windings. Since the effects due to the transformer are relatively small, they may be neglected in estimating the high-voltage short-circuit current flowing to ground in low-voltage distribution circuits due to high-voltage transformer failures. Figure 13 gives a few of the laboratory connections used in the study of high-voltage transformer failures. The arrows represent the detailed current distribution and indicate the compensating primary currents induced by the high-voltage fault current flowing in the low-voltage secondary windings.

It is believed that the total high-voltage short-circuit current produced in low-voltage distribution circuits due to a distribution transformer failure can be determined with an accuracy sufficient for most practical purposes by neglecting the effects of the transformer itself. Additional impedances, such as the resistance of ground return circuit, must be included. It is obvious that the errors due to the approximation decrease as the impedance of the transmission system up to the transformer is increased, and conversely. It is believed this procedure is satisfactory for cases in which the feeder impedance is at least equal to 50 per cent of the transformer leakage impedance in high-voltage ohms. This analysis permits study of the effects of grounding resistances for distribution systems. It gives an approximate method of estimating the voltage to which a distribution system may rise above

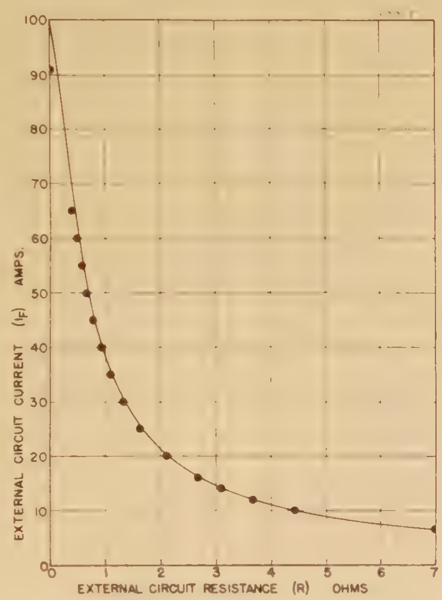


Figure 9. Power-leg-to-neutral experimental short circuits

Transformers connected in wye-delta

ground because of transformer failure, and it is important in the study of protective measures and shock hazard. Experience shows that the number of high-voltage failures in modern distribution transformers are relatively small in comparison to the number in service. However, breakdowns occasionally occur, and a mathematical approach to the problem is of value.

The total fault current in the low-voltage distribution circuit due to a high-voltage transformer breakdown is obtained from the conventional line-to-ground analysis, neglecting the effect of the transformer:

$$I_G=\frac{\sqrt{3}E}{2Z_{TL}+Z_{0TL}+3z} \qquad (19)$$

The voltage rise of the secondary neutral above ground potential due to

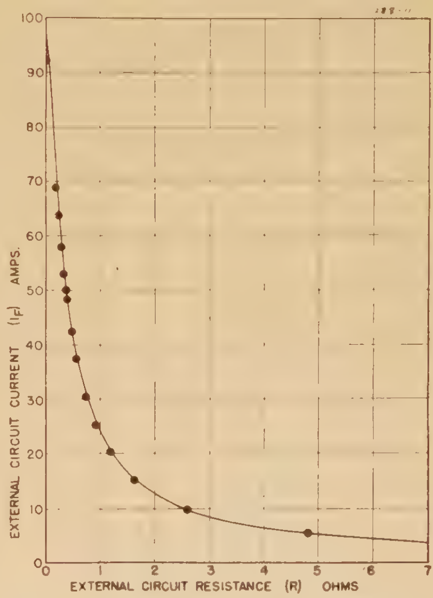


Figure 11. Light-leg-to-neutral experimental short circuits

Transformers connected in wye-delta

this fault current flowing through the impedance to ground equals:

$$E_{LT\ \mathrm{neutral}}=\frac{\sqrt{3}Ez}{2Z_{TL}+Z_{0TL}+3z} \qquad (20)$$

in which z represents the grounding impedance (that is, resistance of driven ground in ohms), and other quantities as defined previously.

Experimental Results

Experiments were made on a laboratory model consisting of three reactors connected to simulate a short transmission line and four ten-kilovolt-ampere transformers having two tapped primary and secondary windings, rated 500/100 volts. The experimental points and theoretical curves for power-leg-to-neutral and light-leg-to-neutral short circuits are given in

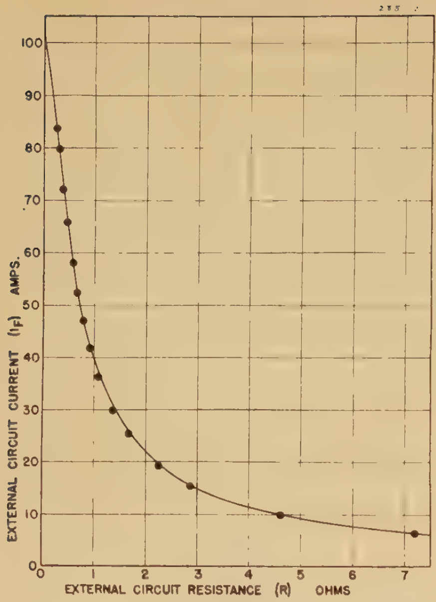
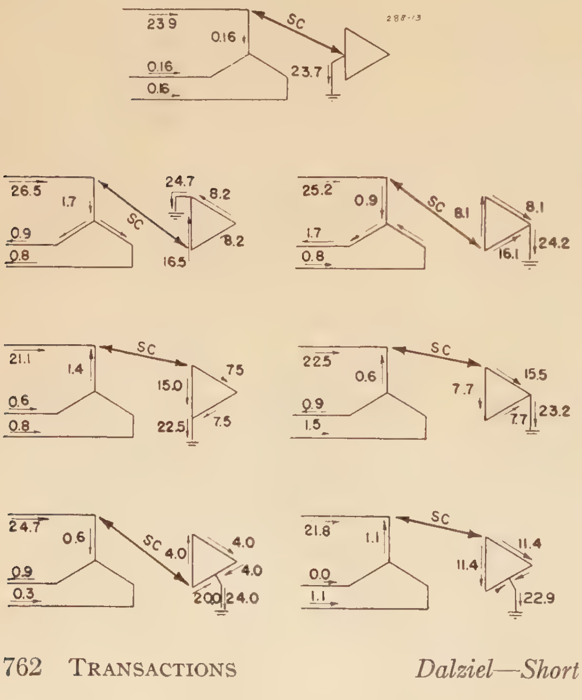


Figure 12. Light-leg-to-neutral experimental short circuits

Transformers connected in delta-delta

Figures 9 to 12 inclusive. The fault or current in the external circuit was plotted against external circuit resistance. The calculations included all known resistances of the circuit, including the resistance of a water-box load connected to simulate the fault, conduit return, and arc resistance. Theoretical and observed currents have also been inserted in Figures 3A, 4A, 6, and 8, to check the determination of high-voltage line currents. The transformers were connected 500 to 200 volts for these tests, and for

Figure 13. Study of effect of transformer in limiting short-circuit current for typical high-voltage transformer breakdowns



Figures 11 and 12 three transformers were used. For the tests of Figures 9 and 10 four transformers were used; two transformers were connected in parallel to simulate the practical case where the lighting transformer had twice the capacity of the power transformers. The agreement between observed and theoretical values was believed satisfactory and is offered as substantiating the analysis. It was noted that the experimental values were slightly less than the computed curves, the discrepancy increasing for smaller values of resistance. The error was believed to be due to the resistance of the many leads and split-terminal socket connections which obviously could not be included in the analysis, to the fact that the system was not entirely symmetrical since the transformers and reactors did not have identical impedances respectively, and to slight unbalances of the supply voltages. The poorest correlation was obtained for the power-leg-to-neutral short circuit with the transformers connected in delta-delta. It should be noted that the discrepancies were on the safe side, as the measured values were low.

Several experiments were made to check the conclusions given in the discussion of high-voltage transformer failures. Short circuits with the transformers energized at reduced voltage were made to simulate primary to secondary failures using three transformers connected 1,000/100, 1,000/200, and 500/500. Typical results for various fault locations for the 1,000/100-volt connection are given in Figure 13. If the winding currents are multiplied by factors proportional to the turn ratio, primary and secondary ampere turns are in close agreement. The slight discrepancies from theoretical values are attributed largely to the effects of saturation and exciting current. This substantiates the statement that as long as compensating currents flow, transformer leakage impedance rather than exciting impedance, is the most important factor in limiting the fault current. As was expected, the effects due to the transformer decreased as the ratio of transformation was increased. The greatest observed error for the 1,000/100-volt connection was five per cent. For these tests the artificial transmission-line impedance was equal to 43 per cent of the transformer leakage impedance, and the ratio of Z_{0TL} to Z_{1TL} was varied between 1.0 and 5.5/1. From this it was concluded that the effects of the transformer may be neglected in approximate determination of the total short-circuit current in low-voltage circuits of practical distribution circuits because of high-voltage transformer failures for ratios of transformation of 10/1 or greater.

Application

For purposes of illustration, the results of this investigation were applied to a typical installation consisting of three 100-kva 2,400/240—120-volt distribution transformers supplied from an infinite bus over a four-kilovolt feeder. The feeder was three-conductor 4/0 copper having the common horizontal spacing of 23–30–53 inches, and $Z_{TL}=0.303+j\,0.622$ ohm per mile. It was assumed that $X_{0TL}=3.5X_{TL}$. The following constants were obtained from tests on one 100-kva unit.

$z_{(S-P)}$ $=0.00691+j0.01895$ ohm or $0.012+j0.0329$ per unit

$z_{(S/2-P)}$ $=0.00277+j0.00528$ ohm or $0.01925+j0.0366$ per unit

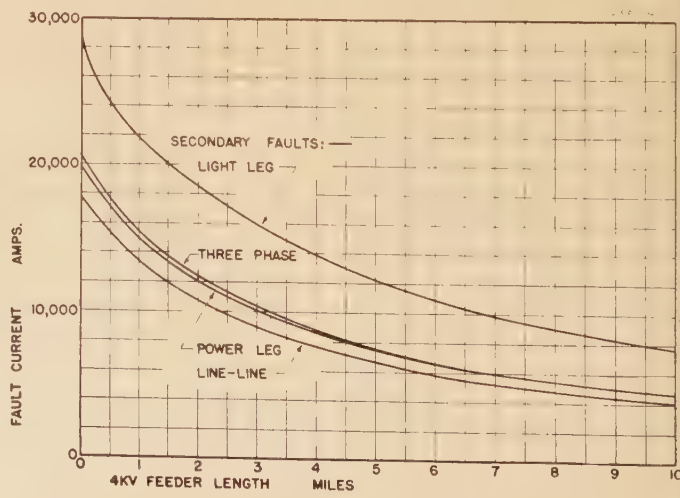


Figure 14. Short-circuit currents for 300-kva, 2,400-wye/240-120-volt transformer and four-kilovolt feeder

Fault resistance assumed zero

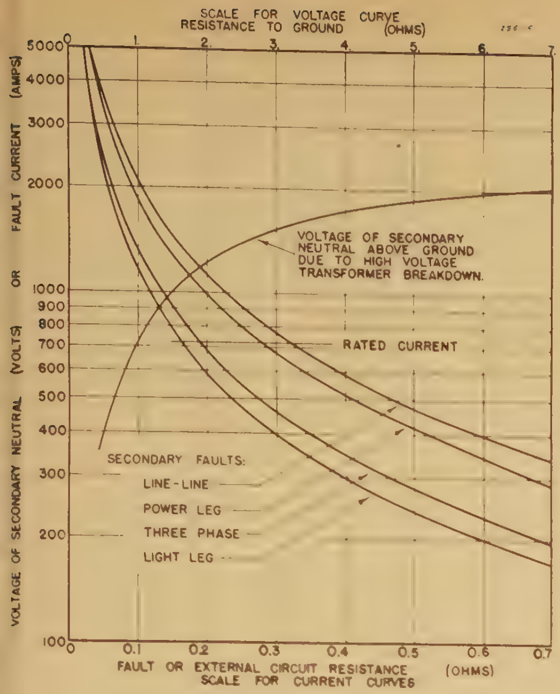


Figure 15. Short-circuit currents and voltage of secondary neutral for 300-kva 2,400 - wye / 240-120-volt transformer and 2.5 miles of four-kilovolt feeder

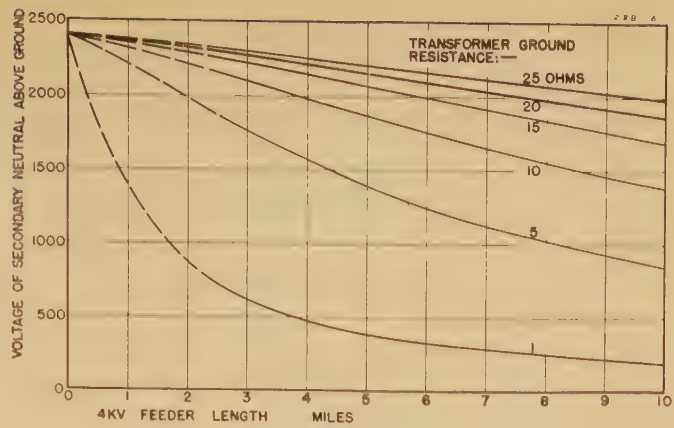


Figure 16. Voltage of secondary neutral above ground due to high-voltage transformer breakdown for 300-kva 2,400-wye/240-120-volt transformer and four-kilovolt feeder

$z_{(s/2-s/2)} = 0.00355 + j$ nil ohm or
0.0246 $+j0.000$ per unit

The transformers were assumed connected in wye-delta with the mid-point of one secondary winding connected to a ground having a variable resistance (R) ohms.

Figure 14 gives results for three-phase line-to-line power-leg-to-ground and light-leg-to-ground short circuits for various lengths of feeder up to ten miles. The fault or ground resistance was assumed zero for this case. It was noted that the light-leg-to-ground fault developed the greatest current, whereas the three-phase and power-leg short-circuit currents were approximately equal. Line-to-line short-circuit currents were the smallest and were equal to the three-phase value multiplied by 0.866.

The effects of fault resistance and resistance to ground are shown in Figure 15. For this case the feeder length was assumed to be $2^1/_2$ miles, and the fault currents were determined for external circuit resistances of 0 to 0.7 ohm. The power-leg-to-ground fault current decreased less rapidly than the light-leg fault current, the latter decreasing from its predominating value for zero resistance to values consistently below that of the other curves. It was interesting to note that if the line-to-line short-circu t current values are sketched in as a function of one-half the fault resistance, the curve practically coincides with the light-leg fault-current curve over the range covered in the figure. The important point brought out by the curves is the large current-limiting effect due to relatively small values of resistance.

The relatively large effect of small impedances in limiting low-voltage short-circuit currents has economic importance. The impedance of current transformers, resistance of joints, and the reactance due to bends in busses and connections may result in a material decrease in the interrupting duty of auxiliary circuit breakers or fuses in comparison with the main circuit breaker. Consideration of these factors in choosing the location of circuit breakers may point to improvements in plant layout with significant savings for large capacity low-voltage installations. Another aspect of this problem concerns the positive operation of circuit breakers or fuses. The protective devices would probably be set to clear for 150 to 250 per cent rated current. Entering the curves with these current values indicates that an external circuit resistance in excess of only 0.06 to 0.10 ohm would prevent proper clearing of faults. The advantage of several subfeeders with low trip values instead of one or two main circuits with high trip values or large fuses is self evident. In cases where high current-rating fuses are required, some form of ground protection (at the main circuit breaker) may be desirable.

The curve showing voltage rise of the low-voltage neutral above ground, due to a high-voltage transformer breakdown, was computed for resistances to ground of 0 to 7.0 ohms. It was found that the fault current would be limited to the rated low-voltage current of the transformer for a ground resistance of about 1.0 ohm. Hence, it was concluded that most transformer failures would be cleared only by the protective devices on the high-voltage side. During the time required to actuate the protective device, the voltage of the distribution system will be subjected to relatively high potential above ground. For example, the voltage above ground reaches 1,500 volts (the minimum dielectric strength of code rubber when new) for a ground resistance of three ohms, whereas a grounding resistance of five to seven ohms provides small protection indeed. The voltage rise of the secondary neutral above ground due to transformer failure for various resistances to ground is plotted as a function of four-kilovolt feeder length in Figure 16.

The results of the study emphasize the importance of securing low resistance to ground and low return-circuit resistances for distribution systems. It is realized that it is often difficult if not impossible to obtain driven or buried grounds having resistances low enough to provide satisfactory protection for low-voltage distribution circuits. This is particularly important for 240-volt three-phase three-wire installations where the mid-tap of one transformer is grounded and the neutral is not run into the plant. The author knows of one such installation consisting of a large transformer bank supplied over a regulated four-kilovolt feeder only a relatively short distance from the substation. The single driven ground at the transformer structure measured about 100 ohms. Although the plant conduits and equipment were apparently well grounded to the water system, little protection was actually afforded against either transformer breakdown or short circuits involving a ground return within the plant. Although the above remarks refer to the examples chosen for study, somewhat similar conclusions might result from analysis of many commercial installations.

Theoretical Possibilities in an Internally Heated Bimetal Type of Thermal Watt-Demand Meter

EDWARD LYNCH
MEMBER AIEE

Synopsis: There are two generally recognized methods of measuring demand. The block-interval type has been used extensively where great accuracy is desirable, a simple explanation necessary, and a quick testing method essential, but the "logarithmic" type has received increased attention in recent years, and a greater dissemination of knowledge of its characteristics is desirable.

A mathematical expression for "logarithmic" average is given which can apply to a variable load as well as to a uniform load. It is shown that a meter with a simple exponential time-deflection characteristic will indicate this "logarithmic" average, but that meters with characteristics obtainable in present commercial thermal meters will theoretically differ from it except when used with certain loads.

The accuracy of commercial meters has increased in recent years. So besides indicating that the various types of characteristics available in commercial meters have apparently been satisfactory on normally encountered loads, it is shown that sturdiness and high torques are desirable features to sustain these accuracies.

The internally heated thermal watt-demand principle is discussed, and the conclusion is reached that by its use high torque can be obtained using the time tested bimetal type of meter. In addition, the rapid diffusion of heat in the bimetal where it is generated theoretically allows shorter "heat-up" time when the meter is being tested and results in a time-deflection characteristic much closer to the ideal simple exponential.

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DEMAND measurements when combined with watt-hour-meter readings have been recognized as giving a practical basis for electricity charges.[1,2] In the United States demand meters operating on the block-interval principle have been extensively used, and their operating principle and characteristics are well understood.

There is another recognized method of measuring demand which has been used to a limited extent for the same purpose. It is the method utilized by thermal demand meters, and the resulting indication has been called the "logarithmic average" demand. A general explanation of meter design considerations which enter into this demand indication and the operating characteristics which they affect will be covered in the following discussion.

Time Deflection Characteristic

The block-interval meter indicates continual arithmetic average maximum demand. The thermal meter obtains its average in a different manner, and it indicates a slightly different quantity which can be designated as continuous logarithmic average maximum demand. On a constant load its reading changes rapidly at first and then more slowly until, in the case of a meter which reaches 90 per cent of its indication in 15 minutes, which by definition makes it a 15-minute demand interval meter, it would be within about one per cent of its ultimate in 30 minutes. From this characteristic we see that any load on the meter during the 30 preceding minutes may have a readable influence on the demand pointer indication, but that the longer time that has elapsed, the less effect this load has on the reading. The indicating pointer on these meters is therefore continuously changing its position as load changes occur according to what has been called a "logarithmic" average.

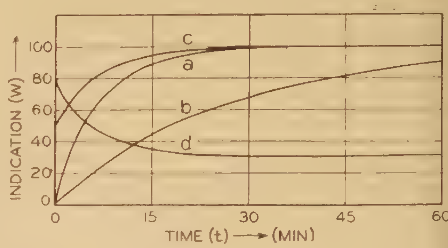

Figure 1. Curves of $W=W_1(1-\epsilon^{-kt})+W_0\epsilon^{-kt}$

	W_1	W_0	K
a	100	0	0.1535
b	100	0	0.0384
c	100	50	0.1535
d	30	80	0.1535

This average for a theoretical meter can be expressed as follows (see appendix B):

$$W=k\int_{-\infty}^{t} w\epsilon^{-k(t-t_1)}dt_1 \qquad (1)$$

where

W = instantaneous deflection of pointer
k = a design constant
w = load (which may vary with time)
t = time at which deflection is W
t_1 = time at which load is w

On constant loads after about three demand intervals this "logarithmic" average is identical with the arithmetic average as given by a block-interval type of meter.

This study would hardly be complete without one or two constructive suggestions. The goal must be as low a resistance to ground as possible. The resistances of the ground circuit must be maintained sufficiently low to insure positive operation of the protective devices in case of transformer failure. In addition, it would appear that considerable protection would result if, in addition to providing the best ground that can be reasonably obtained at the transformer installation, the low-voltage neutral be carried along with the phase wires (regardless of whether or not it is to be used for power) and connected to the plant conduit system and grounded as effectively as possible on the customer's premises. The common neutral system of grounding would appear to provide an alternate method of accomplishing the same result.

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Simple Exponential Is Ideal

However, time-deflection characteristics of commercial thermal meters are not identical, and they differ from the characteristics of block-interval meters so that on fluctuating loads they can theoretically give slightly different readings. The following discussion of theoretical and practical time-deflection curves will indicate the desirability of a simple exponential characteristic for the thermal meter.

Figure 1 shows typical theoretical time-indication characteristics of thermal meters when a constant load is applied. Curves a and b differ by the fact that the demand interval or time to reach 90 per cent indication is 15 minutes (k is 0.1535) for a, and 60 minutes (k is 0.0384) for b. Curves c and d have 15-minute demand intervals but start with an initial indication. Each curve can be expressed by the "exponential" equation:

$$W = W_1(1-\epsilon^{-kt}) + W_0\epsilon^{-kt} \qquad (2)$$

(See appendix A)

where

W = instantaneous deflection of pointer
W_0 = deflection of pointer at zero time
W_1 = ultimate deflection for load involved
ϵ = 2.718
k = a design constant
t = time

(W, W_0, W_1 must be expressed in the same units, and k must be in units the reciprocal of the units for t.)

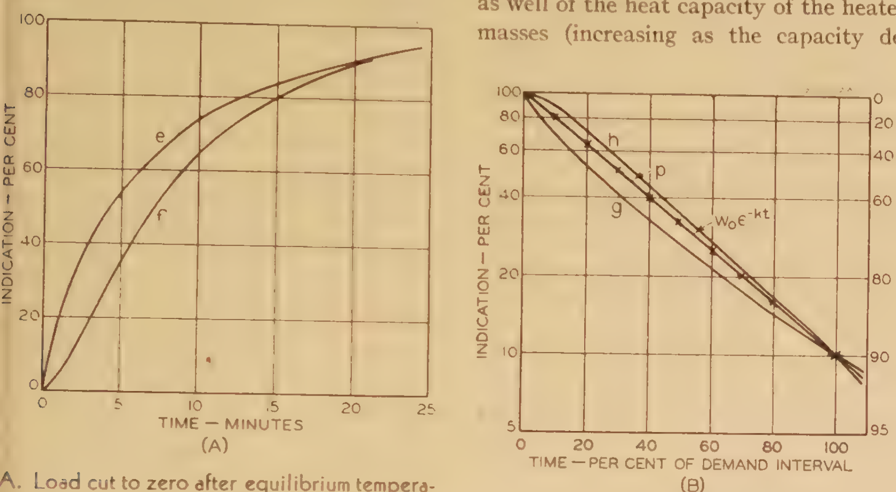
The well-known expressions $W = W_0\epsilon^{-kt}$ and $W = W_1(1-\epsilon^{-kt})$ for the transient return of the indication to zero when power is cut off, and the rise to ultimate deflection when a constant load is applied from zero deflection, can be seen to be special cases of the above more general equation. Equation 2 holds for conditions of constant applied load, whether or not the indication starts or ends at zero, and whether the indication increases or decreases. It is interesting to note that the instantaneous deflection is equivalent to the sum of two hypothetical indications, one of which starts at zero and rises to the ultimate deflection of the meter, and the other of which starts at the initial indication of the meter and reduces to zero, both following the locus given by equation 2.

It is also interesting to note that k, which determines the demand interval has a dimension equivalent to the reciprocal of time and is a function of the heat insulation of the heated parts (increasing as the insulation decreases), and a function as well of the heat capacity of the heated masses (increasing as the capacity decreases). This same k represents in magnitude the reciprocal of the time that would be necessary to attain proper deflection if there were no heat escape. It also represents the reciprocal of the time necessary to accomplish a change in indication represented by the following fraction of the total change:

The fraction is:

$$\left(1-\frac{1}{\epsilon}\right) = 0.632 \text{ approximately}$$

If the load varies at a uniform rate (see appendix B), either increasing or decreasing, k has further physical significance because $1/k$ represents the time between the arrival at some particular watt load and the indication of that load by the meter, providing the rate has been uniform long enough (three demand intervals) for this time lag to become essentially constant. This time lag is independent of the rate of change of watts and the meter demand interval is directly proportional to it if we consider simple exponential characteristics.



A. Load cut to zero after equilibrium temperature is reached

B. Constant load applied from zero

Figure 2. Time-indication curves of typical thermal watt-demand meters

Time-Deflection Curves in Meters

No commercial meter in the past has, to the writer's knowledge, attained exactly an indication given by equation 2. Curves e and f in Figures 2A and 2B represent typical time-deflection curves of actual meters which have been converted to equivalent demand intervals in g and h and are plotted decreasing from an initial value on semilog co-ordinates in order to easily compare them with the straight line p of $W_0\epsilon^{-kt}$.

An investigation of time-indication curves on any particular type of thermal meter indicates that there are small variations from meter to meter in the demand interval due to limitations in manufacturing control. We find also that in the same meter there are differences depending on the conditions of load and ambient temperature. We can reason from this that for a "15-minute demand interval" meter the actual demand interval should not be less than 15 minutes under any normal conditions of use in order not to read high on certain conditions of load, and we find practically all meters on the market which are considered satisfactory today for use as 15-minute demand interval meters fall within limits from 15 to 21 minutes interval.

When we consider differences in the curves in Figures 2A and 2B, we find another fundamental characteristic which although small might appreciably affect the meter readings in certain isolated conditions. This effect is easily demonstrated by applying a load which would give about full-scale deflection in 40 minutes and cut the load off at the end of five minutes. If we consider the "exponential" characteristic as shown in p as a standard of comparison, we find that a meter with a characteristic similar to e may read high by eight per cent of full scale, and a meter similar to f may read low by eight per cent at the instant the power is cut off. These should not be considered as errors as the definition of the time-deflection characteristic determines these differences. The meter following the d curve will continue to rise for a

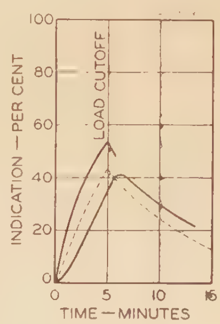


Figure 3. Time-indication curves for typical thermal watt-demand meters

Uniform load cut to zero before thermal equilibrium has been attained

little time after the power is cut off but will not quite reach the maximum value reached by the "exponential" characteristic. Figure 3 shows this characteristic graphically. It will be noted that meters with the *f* characteristic lag behind changes in load, while meters with the simple "exponential" or the *e* type of characteristic will change direction immediately in the direction of the new load value.

Simple Exponential Curve Desirable

From this brief discussion we can reason that as far as thermal meters are concerned, subject, of course, to any considerations which rate engineers may find of importance, the simple "exponential" time-indication curve published as characteristic of thermal meters is desirable. It is desirable if an acceptable time-indication curve is to be defined, for it allows a relatively simple mathematical definition of the shape of the curve. And more important is the fact that it is the only characteristic which will indicate "logarithmic" average for all types of loads.

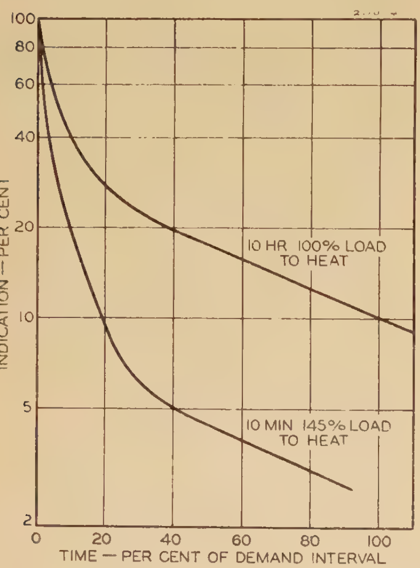


Figure 4. Time-indication curves of special thermal watt-demand meter

Cooling curves after loads as shown

Multibranch Thermal Circuits

The reason for the divergence of commercial-meter characteristics from the simple exponential has been attributed to diffusion.

Figure 4 shows performance curves of a 60-minute-interval thermal wattmeter illustrating an exaggerated condition of this diffusion which appeared in a paper presented by P. M. Lincoln before the Institute. The design constant, k was treated as a variable to account for the results. A slightly different treatment of the curves may be of interest and offers some advantages from a mathematical point of view in determining their shape.

The analogy shown in Figure 5 illustrates that multiple-branch thermal circuits may be expected to cool in a constant ambient according to the exponential:

$$W = W_0(A_1\epsilon^{-k_1t} + A_2\epsilon^{-k_2t} + A_3\epsilon^{-k_3t} \ldots + A_n\epsilon^{-k_nt}) \quad (3)$$

We can use the following expression to predict changes in indication when any constant load is applied:

$$W = W_0(A_1\epsilon^{-k_1t} + A_2\epsilon^{-k_2t} \ldots + A_n\epsilon^{-k_nt}) + W_1(1 - A_1\epsilon^{-k_1t} - A_2\epsilon^{-k_2t} \ldots - A_n\epsilon^{-k_nt}) \quad (3a)$$

In these equations A_1, A_2, k_1, k_2, and so forth are complicated functions of the storages and heat-transfer coefficients of the various branches. The form $W_0\epsilon^{-kt}$ referred to in various literature on thermal demand meters has been referred to above as a special case of equation 2, and equation 2 can be seen to be a special case of this more general equation 3a.

P. M. Lincoln has shown that the simplified expression is inadequate to explain the results of his tests unless we treat k as a variable. However, the results shown in Figure 4 can be predicted approximately from an equation such as 3, carried out to three terms as below:

$$\text{Indication} = W_0(0.19\epsilon^{-0.921t} + 0.51\epsilon^{-0.2302t} + 0.3\epsilon^{-0.019t}) \quad (4)$$

This curve is plotted in Figure 6 where the first term is given as a dotted line, the second term as a dash line, and the third term as a dot and dash line, and the total as a full line. This curve checks reasonably with the per cent indication versus time curves (after ten-hour heating). Another group of curves (Figure 7) shows the heating curve for ten minutes at 145 per cent load and the following cooling curve when the load is removed, as given by constants in equation 4. Crosses indicate points calculated by considering the multiple-branch circuit as used in equation 4, and the circles represent points taken from Doctor Lincoln's meter data. The correlation is considered reasonably good for a first approximation of coefficients and exponents in the above equation. This allows us to think of meters of this nature as if they were the equivalent of multiple-branch thermal circuits with more than one constant k. Thus although such a meter reaches about 90 per cent of its final indication in 60 minutes, its final indication within reading limits will depend on the coefficient and power terms of one or more of the terms in equation 3. This may result in an extremely long time when considered from a testing point of view. In the above equation 4 for the 60-minute characteristic shown in Figure 6, k_1 corresponds to a demand interval of about 2.5 minutes and k_2 to an interval of about ten minutes and k_3 to an interval of about two hours. Doctor Lincoln has shown that meters with performance closer to the simple expression $W = W_0\epsilon^{-kt}$ can be built.

Time-Indication Curves of "Square" Scale Meters

When we consider the time-indication curves of a demand meter with a "square"

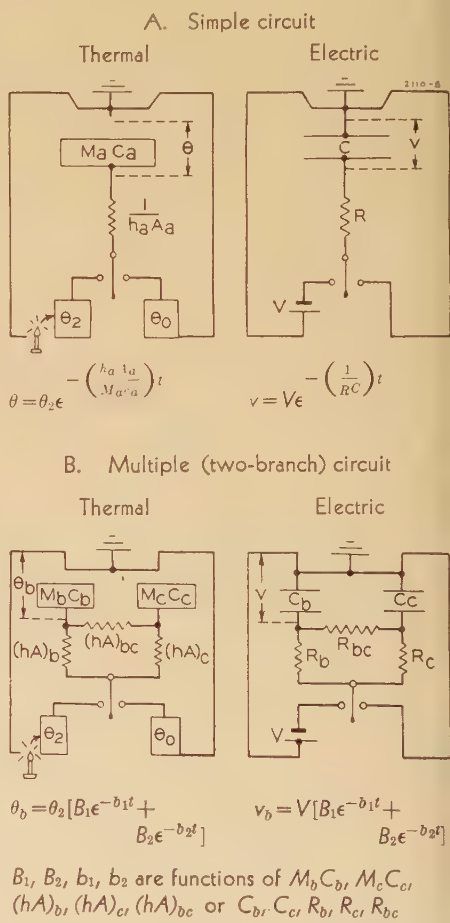


Figure 5. Comparison of thermal and electric circuit

M = mass
c = specific heat
C = capacitance
h = heat transfer coefficient
A = area
R = resistance
θ = temperature difference
v = voltage
θ_2 = initial temperature difference
V = initial voltage
θ_0 = ambient temperature

scale such as those used for ampere demand, we encounter further characteristic differences. The time-deflection characteristic of the wattmeter (as a simple thermal circuit) in general form has been given in equation 2 for constant applied loads. The time-deflection characteristic of a "square" scale meter can be expressed similarly, but since the indication is not proportional to the deflection but is proportional to its square root, we find the expression for indication:

$$A=\sqrt{A_0^2\epsilon^{-kt}+A_1^2(1-\epsilon^{-kt})} \qquad (5)$$

where

A = instantaneous indication on a "square" scale (constant load)
A_0 = initial indication on a "square" scale
A_1 = final indication (constant load)

This reduces $A_0\epsilon^{-k/2t}$ when load is cut off. This has the same form as $W_0\epsilon^{-kt}$, and the cooling curve of time-indication will have the same general shape as the wattmeter, but since the exponent is $k/2$, it will take twice as long to cool to any percentage indication.

If the load starts at zero indication equation 5 reduces to

$$A=A_1\sqrt{1-\epsilon^{-kt}}$$

It can be seen that this does not have the same form as $W=W_1(1-\epsilon^{-kt})$, which is the equivalent on a uniform scale. These curves are shown graphically in Figure 8.

It may seem desirable to keep the shape of the time-indication curves of "square" scale and uniform scale meters on increasing loads (starting at zero) as nearly alike as possible. It has been proposed to decrease k for the ammeter to half the value for a wattmeter. This gives curve n shown in Figure 8 (as compared to curve j). It also gives a cooling curve m which is indeed slower (compared to i). Such a meter not only will differ in basic construction but also will theoretically differ on varying loads when compared to the uniform scale meter.

Another alternative suggested is to utilize the design and construction of the wattmeter, which requires changing the definition of demand interval so that the fraction of final indication of a "square" scale meter is the square root of that of a uniform scale meter. Thus, if the wattmeter is kept at 90 per cent for the demand interval definition, the ammeter would then be $\sqrt{0.90}=0.95$ or 95 per cent. Such a meter does not have the same characteristic as a uniform scale meter, and its reading will differ on varying loads when compared to the uniform scale meter. (See k and l of Figure 8.) However, if the basic characteristic of interest is the heating effect, then the characteristic of this meter favorably compares with that of a uniform scale wattmeter. Perhaps in such a case, the scale marking should be in terms of amperes squared.

It is interesting to note that differences of watt- and ampere-demand meter readings on normally encountered loads have been sufficiently small to allow the use of meters with many of the varying characteristics described above with satisfactory results.

Operating Characteristics

With a knowledge of the fundamental operation characteristic of thermal meters, let us consider how it is affected with time or with changing conditions of load power factor, ambient temperature, and so forth. One of the important considerations in reliability of a meter of this kind is mechanical stability in which sturdiness of construction and constant friction are important factors. These factors are especially significant in meters which are to be maintained with a minimum of skilled servicing and equipment. Most meters with maximum indicating pointers have friction clutches to hold one pointer in the position of its maximum indication. Experience over a long period of time has shown that there is a more or less definite range of optimum friction in such cases. This is due to the fact that over the range that is normally used, the friction becomes more constant, the greater the magnitude of the friction. However, at the higher values of friction, its magnitude is objectionable, and at the very low values its variation with time, temperature conditions, and so forth, is equally objectionable. A value from 30 gram-millimeters

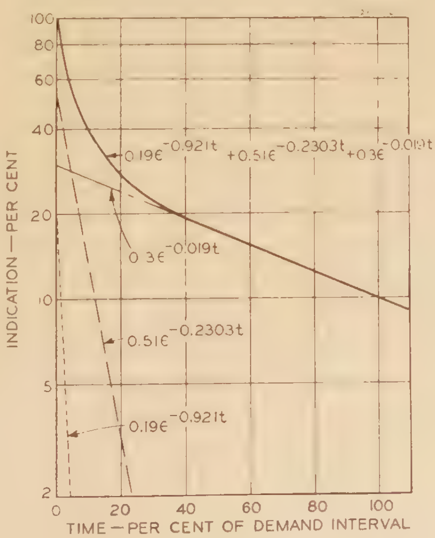


Figure 6. Time-indication curve including components

up, in a design which has been used for many years, has proved very satisfactory as far as consistency is concerned.

High Torque Desirable

To utilize frictions of this order of magnitude and still have them only approximately $1^1/_2$ or 2 per cent of the full-scale meter torque requires a meter torque of 1,500 to 2,500 gram-millimeters or a torque gradient of about 20 to 35 gram-millimeters per degree. Most thermal meters on the market today of the bimetal type do not utilize such high torques and rely on the lower friction torque remaining as constant as is required. The limiting features in obtaining higher torques are:

1. The amount of bimetal that is used, which is limited by other considerations such as space requirements, and so forth, and which has a definite influence on the demand interval of the meter.
2. The temperature at which the radiation or convection becomes an appreciable factor and the maximum temperature that the structure can stand.

To increase the operating temperature of the temperature-sensitive element without raising the temperature of the insulating parts of the heater requires better efficiency of heat transmission from the heater to the heated element. In recent years progress has been made toward greater efficiency and at the same time toward limiting convection and radiation by resorting to a bourdon tube actuating element with its bulb covered by a thermos bottle. This has gone a long way toward the achievement of high torque with low watts input. Inherent in a bourdon-tube design, however, is the hazard of leakage, one that must be eliminated by proper design, taking into consideration the long ranges of loads, power factors, and ambient temperatures over which modern meters are expected to function, and realizing the pressures that are set up in the tubes to meet these conditions. Added to this problem is one of choosing the best liquid for the bourdon tube. The problem is to obtain one which will not cause difficulty at lower temperatures and, at the same time, will have satisfactory temperature-expansion characteristics over the range required. With the use of demand meters in outdoor installations, this limiting feature assumes importance for temperatures below 32 degrees Fahrenheit and more especially, for temperatures as low as −40 degrees Fahrenheit, which although perhaps not common, might certainly be encountered in some installations. Many liquids are available and much experimental work has been done in this direction.[3]

Internally Heated Bimetal System

But when we consider difficulties of this nature together with others such as the hysteresis effects in bourdon-tube springs, and so forth, we realize the desirability of finding another method of attaining better heat-transmission efficiency which can be used with the time-tested bimetal spiral construction. Such a method is available if we heat the element by current through the bimetal itself. Here no loss is encountered, because the heat is generated where it is wanted. We thus get the highest possible efficiency of transmission and can limit radiation by means of controlled surfaces and convection by means of limited air spaces which, because of research work done in recent years, offers a practical solution.

If this arrangement is utilized to obtain the maximum efficiency of heat transmission possible, full-scale torques of the order of 2,000 grams-millimeters are readily obtained, and we can continue to use the time-tested bimetals as the activating element, where past experience and the vast amount of work that has been done by bimetal companies and meter manufacturers on heat treatment and the aging process insures stability of the bimetal characteristics. Moreover, uniformity of bimetal characteristics has recently been the subject of intensive investigation and has resulted in closer perfection of bimetal spirals with far more consistent characteristics than have been obtainable in the past.

Effect of Temperature Coefficient of Bimetals

One of the interesting phases encountered in taking advantage of the internally heated principle is occasioned by the fact that all suitable bimetals have a positive temperature-resistance coefficient. This has led to the following equations from a simple mathematical solution of Figure 10 giving the indication of such a meter:

$$W = 4I_E I\cos\theta\left[\frac{R_1R_2}{R_1+R_2}\right] + I_E{}^2[R_1-R_2] - I^2\left[\frac{R_1R_2(R_1-R_2)}{(R_1+R_2)^2}\right]$$

Since

$$I_E = \frac{E}{R_1+R_2}$$

Therefore:

$$W = 4EI\cos\theta\left[\frac{R_1R_2}{(R_1+R_2)^2}\right] + E^2\left[\frac{R_1-R_2}{(R_1+R_2)^2}\right] - I^2\left[\frac{R_1R_2(R_1-R_2)}{(R_1+R_2)^2}\right]$$

If:

$R_1 = aR_2$ where a may be a variable as load conditions vary

$$W = 4EI\cos\theta\left[\frac{a}{a+1}\right] + \frac{E^2}{R_2}\left[\frac{a-1}{(a+1)^2}\right] - I^2R_2\left[\frac{a(a-1)}{(a+1)^2}\right] \quad (6)$$

This general equation enables us to make a selection of a value for a when the meter is not excited and a value for the ratio of I to I_E which will give satisfactory values for W when $EI\cos\theta$ is kept constant, and ambient temperature, voltage, power factor, or load is changed through limits ordinarily met in service. These are two design constants which the design engineer can vary to obtain the optimum condition for this sort of meter.

When a is equal to 1.0, the last two terms of this equation disappear. However, if a differs slightly from 1.0 we can take advantage of the changes in these last two terms with temperature, power factor, and so forth, to obtain some slight compensation for small inherent errors.

An important bimetal characteristic which enables a simple mechanical construction to meet these requirements is the fact that the torque per degree temperature rise is independent of the length of a bimetal coil. We can therefore buck two bimetal coils on a single shaft to obtain complete temperature compensation at no load, and at the same time get the required ratio in resistances by using coils of different lengths. Modern bimetal-coil manufacturing procedure has made possible the satisfactory matching of coils of this nature.

Data on Actual Meter

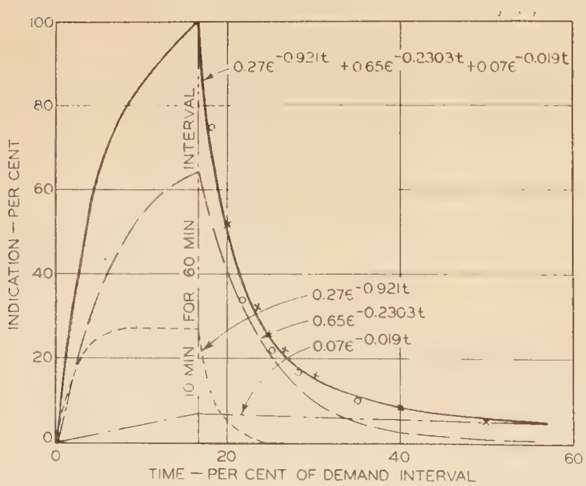
It is interesting to note how these observations are confirmed in a sample meter built to take advantage of this high efficiency heat generation thus giving a high torque bimetal meter. Figure 9 illustrates such a meter and Figure 10 gives its wiring diagram. The most important operation characteristics are tabulated in Table I and indicate the possibilities which may be utilized in development along these lines. Important is the fact that generation of heat in the bimetals allows such rapid diffusion of heat that there are possibilities that the necessary voltage heat-up before test can be shortened and that the time deflection curves actually will follow the desirable simple exponential curve.

Conclusions

A great deal must be discussed in elaborating on the various time-deflection characteristics obtainable in thermal watt-demand meters but experience has indicated that since demand is not an exact quantity, and since rate structures must vary for different classifications of load,

Figure 7. Time-indication curves

Constant load cut to zero before thermal equilibrium has been attained (showing components)



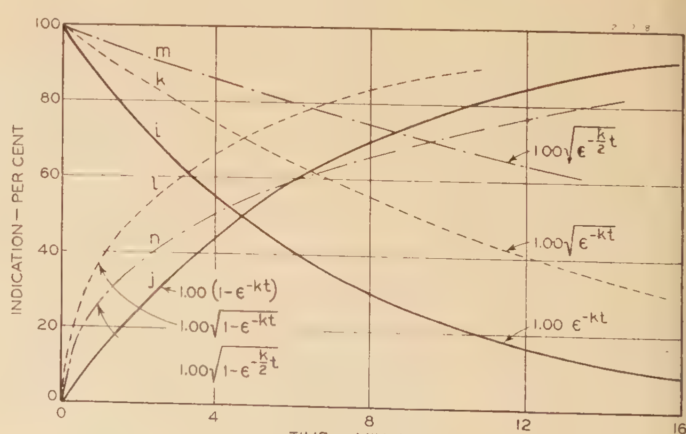


Figure 8. Time-indication curves

Two types of "square" scale meters compared to a uniform scale meter

the various kinds of characteristics discussed above all seem to be satisfactory for most demand measurements.

Accuracy of thermal meters today is considerably better than it was several years ago, and sturdiness, together with high torque, are desirable qualities to sustain this greater accuracy.

This high torque is obtainable with the time-tested bimetal type of thermal watt-meter by taking advantage of the "internally heated" principle which gives maximum heat transfer efficiency and gives a time of response characteristic much closer to the ideal simple exponential than has been attained in commercial meters in the past.

The preceding data of a typical meter developed to give high torque by means of internally heated bimetal construction indicates that a superior all-around meter has been designed, and that this principle of operation is worthy of consideration as the basic design of thermal watt-demand meters.

Appendix A[6]

The general time-deflection response equation is developed here for a device indicating the difference in temperature of two similar bodies connected by a thermal shunt when constant heat inputs (or zero input) is applied to each body, the bodies starting at any initial temperature or temperature difference.

Let

H_1 = rate of heat applied to A (gram calories per second)

H_2 = rate of heat applied to B (gram calories per second)

h = thermal conductivity from A or B to ambient (gram calories per second per degree centigrade)

Q = thermal conductivity of shunt (gram calories per second per degree centigrade)

θ_{01} = initial temperature of A (degrees centigrade)

θ_{02} = initial temperature of B (degrees centigrade)

θ_1 = instantaneous temperature of A above θ_{01} (degrees centigrade)

θ_2 = instantaneous temperature of B above θ_{02} (degrees centigrade)

M = heat stored in A or B per degree centigrade (gram calories)

The conservation of energy equations become

$$H_1dt-Q(\theta_1-\theta_2+\theta_{01}-\theta_{02})dt=h(\theta_{01}+\theta_1)dt+Md\theta_1$$

$$H_2dt+Q(\theta_1-\theta_2+\theta_{01}-\theta_{02})dt=h(\theta_{02}+\theta_2)dt+Md\theta_2$$

Let

$$\theta_{01}-\theta_{02}=W_0 \text{ and } (\theta_{01}+\theta_1)-(\theta_{02}+\theta_2)=W \text{ and } \theta_1-\theta_2=W_2$$

$$d\theta_1+d\theta_2=d(\theta_1+\theta_2)=dW_2$$

The time-deflection response of thermal meters is proportional to the temperature difference W.

Then by subtraction and substitution

$$(H_1-H_2)dt-2Q(W_2+W_0)dt=h(W_2+W_0)dt+MdW_2$$

$$\frac{dW_2}{dt}+\frac{h+2Q}{M}W_2=\frac{H_1-H_2}{M}-\frac{h-2Q}{M}W_0$$

Let

$p=d/dt$, and $\mathbf{1}$ = Heaviside's unit operator (see reference 4)

$$\left[p+\frac{h+2Q}{M}\right]W_2=\left[\frac{H_1-H_2}{M}-\frac{h+2Q}{M}W_0\right]\mathbf{1}$$

$$W_2(t)=\left[\frac{H_1-H_2}{M}-\frac{h+2Q}{M}W_0\right]\times\left[\frac{1}{p+\frac{h+2Q}{M}}\right]\mathbf{1} \quad (1)$$

Figure 9. Single-phase watt-demand meter

15 amperes, 240 volts, 60-cycle, three-wire

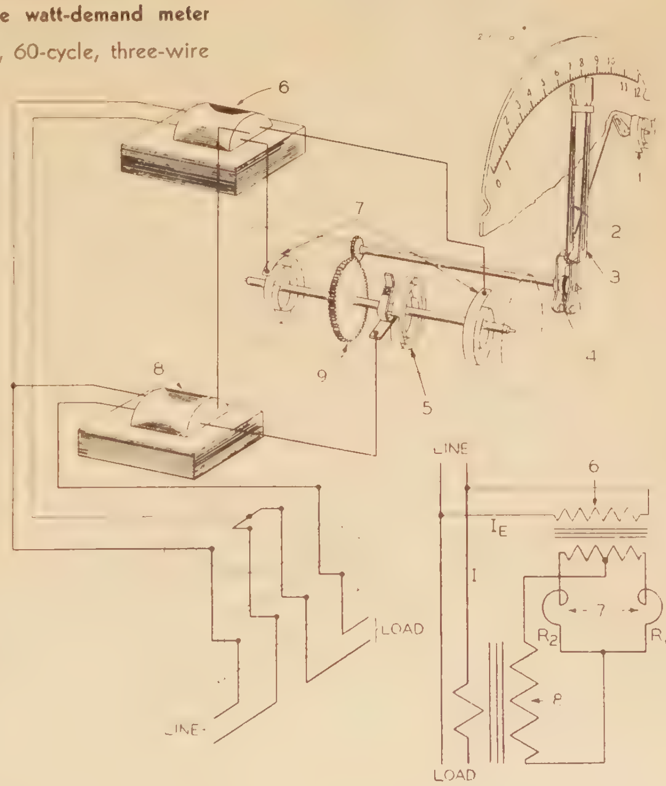


Figure 10. Wiring diagram for single-phase watt-demand meter

1. Calibration adjustment
2. Kilowatt indicator
3. Maximum - demand indicator
4. Friction clutch
5. Zero adjustment spring
6. Potential transformer
7. Thermostat metal
8. Current transformer
9. Drive gear

From simple operational calculus

$$W_2=\left[\frac{H_1-H_2}{M}\right]\left[\frac{M}{h+2Q}\right]\times\left[1-\epsilon^{-\frac{h+2Q}{M}t}\right]-\left[1-\epsilon^{-\frac{h+2Q}{M}t}\right]W_0$$

By definition above

$$W=W_2+W_0$$

$$W=\left[\frac{H_1-H_2}{h+2Q}\right]\left[1-\epsilon^{-\frac{h+2Q}{M}t}\right]+W_0\epsilon^{-\frac{h+2Q}{M}t}$$

Let

$$\frac{H_1-H_2}{h+2Q}=W_1 \text{ and } \frac{h+2Q}{M}=k$$

Then

$$W=W_1(1-\epsilon^{-kt})+W_0\epsilon^{-kt} \quad (2)$$

Appendix B

The response of an object such as a theoretical thermal watt-meter is developed here for three conditions of loading:

(*a*). Where the load is any function of time.

(*b*). Where the load is constant.

(*c*). Where the load increases at a constant rate.

The response is of such a nature that it is directly proportional to the temperature of a mass where the mass is supplied with a heat input proportional to the load; and this input is utilized both in raising the temperature of the mass and in supplying the heat escape which is proportional to the temperature of the mass.

Let

h = thermal conductivity from mass to ambient (gram calories per second per degree centigrade)

Table I. Characteristics of a 15-Ampere 240-Volt Three-Wire Single-Phase Thermal Watt-Demand Meter

(Typical Average Values for an Experimental Internally Heated Design)

Torque (full-scale)	2,200 g-mm
Scale length	Approximately 4 inches
Power factor influence (7.2 kw) referred to pf 1.00 — 0.5 pf lag	Less than 2%
— 0.5 pf lead	Less than 2%
Frequency influence referred to 60 cycles	Negligible
Temperature influence (12.0 kw, 1.0 pf) referred to 25 C — +50 C	Less than 2%
— −20 C	Less than 2%
Voltage influence (10% change in voltage) referred to rated voltage	Less than 2%
Rising time-deflection curve shape	"Simple exponential" (Figure 2B, curve p)
Demand interval	Approximately 17 minutes
Watts loss — Potential (240 volts)	2.5
— Current (12 kw, pf 1.0)	9.0
Size	Standard single-phase watt-hour meter size

$hf(t_1)$ = rate of heat applied (gram calories per second)

θ = temperature (instantaneous) of mass which is proportional to meter deflection (degrees centigrade)

t = time (minutes)

W = angular deflection

The conservation of energy equation becomes

$$hf(t_1)dt = h\theta dt + Md\theta$$

Then

$$\frac{d\theta}{dt} = \frac{h}{M}[f(t_1) - \theta] = k[f(t_1) - \theta] \text{ where } k = \frac{h}{M}$$

Let

$\frac{d}{dt} = p$, and $\mathbf{1}$ = Heaviside's unit operator (see reference 4)

$$(p+k)\theta = kf(t_1)\ \mathbf{1}$$

$$\theta = \frac{k}{p+k}[f(t_1)]\mathbf{1} = k\left[\frac{1}{p}\right]\left[\frac{p}{p+k}\right][f(t_1)]\ \mathbf{1}$$

From Borel's theorem:

$$Y_1(p) = \frac{p}{p+k}\mathbf{1} = \epsilon^{-kt_1}$$

$$Y_2(p) = f(t_1)\ \mathbf{1}$$

$$\theta = k\int_0^t \epsilon^{-k(t-t_1)} f(t_1)dt_1 \tag{1a}$$

This shows that the temperature θ of the indication W is proportional to the sum of all the instantaneous values of $f(t_1)$ each multiplied by a factor, $\epsilon^{-k(t-t_1)}$, which decreases exponentially as the time, t_1, of the instantaneous value of $f(t_1)$ is taken backwards from the time, t. The value t is the time of the instantaneous value of the indication W or temperature θ being considered.

(a). This function,

$$\kappa\int_0^t [f(t_1)][\epsilon^{-k(t-t_1)}]dt_1$$

can be designated by definition as the logarithmic average of $f(t_1)$ at any time, t, and is the value a simple theoretical thermal watt-demand meter indicates.

(b). If $f(t_1)$ is a constant such as W_1 after starting from zero indication, the expression becomes

$$W_1(1-\epsilon^{-kt})$$

When the transient ϵ^{-kt} becomes small, the meter will read W_1 or the equivalent of a block interval type of meter.

(c). But if $f(t_1)$ is directly proportional to time such as At the expression reduces to

$$At - \frac{A}{k} + \frac{A}{k}\epsilon^{-kt}$$

This indicates that after the transient $A/k(\epsilon^{-kt})$ becomes small (about three demand intervals), the meter will have approached a constant increase in deflection:

$$\frac{d\theta}{dt} \text{ when } \epsilon^{-kt} \text{ is small} = \frac{d}{dt}\left(At - \frac{A}{k}\right) = A$$

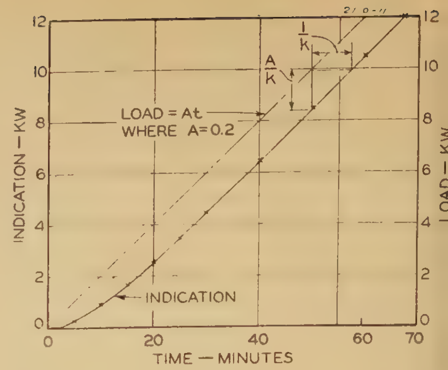


Figure 11. Indication lag of a thermal watt-demand meter on a constantly increasing load

x—points for curve

$$At - \frac{A}{k} + \frac{A}{k}\epsilon^{-kt}$$

where $A = 0.2$

$k = 0.135$

(demand interval = 17 minutes

Also since the deflection, if it followed watts input, instantaneously would be proportional to At.

And since the deflection, after the transient term becomes small, is $At - A/k$; therefore the reading will lag in deflection by A/k, and since the rate of change of deflection is A, the reading will lag in time by $1/k$.

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Practical Calculation of Circuit Transient Recovery Voltages

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WHILE there have been a number of papers on recovery voltage and its effect on the operation of circuit-interrupting devices, and although the transient recovery voltage and its characteristic are assuming a more important place in the design and application of circuit breakers with the growth and increasing capacity of power systems, there has not been available to the average power-company engineer a convenient method for determining this characteristic. The present paper offers such a method and also tabulates capacitance data for the more important circuit elements.

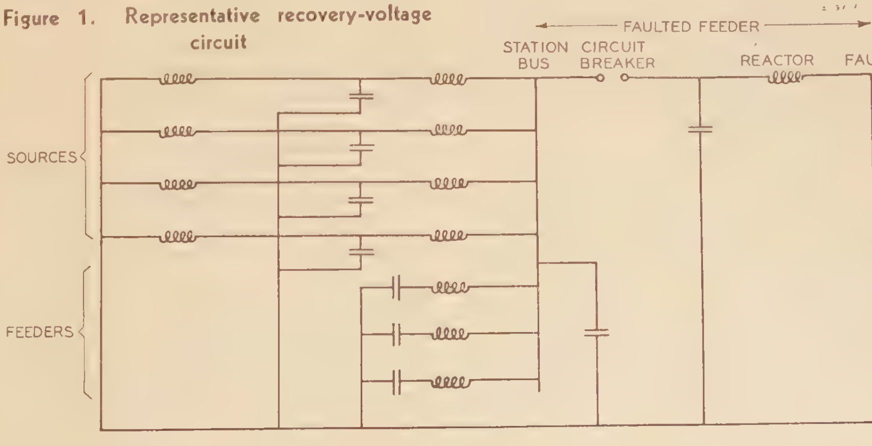
This method was developed in connection with a survey conducted by the Association of Edison Illuminating Companies to obtain a comprehensive picture of existing circuit recovery-voltage conditions. In order to review the characteristics for practically all breaker locations on the six power systems studied, it was necessary to develop a method that could be applied with minimum effort and time. The results of this survey are given in the companion paper, "Transient Recovery-Voltage Characteristics of Electric-Power Systems," by H. P. St. Clair and J. A. Adams.[11]

For different locations having the same voltage and short-circuit current duties, the transient recovery characteristic may vary with high-voltage values of approximately the same magnitude occurring over a time range of almost one hundred to one. From one end of this range to the other considerable differences may occur in the performance of some types of breaker. However, a moderate change in time to the first high peak usually produces a comparatively small change in breaker performance; a change in this time of ten to one, for instance, might produce a two-to-one change in arc length. In this situation it will be obvious that high accuracy is not required in the determination of the recovery transient, so that approximations which might be intolerable in some other applications become here quite permissible. This is very fortunate, for the circuits involved are far too complex for precise mathematical analysis and in many cases do not lend themselves well even to miniature system analysis. With a certain amount of approximation, however, these circuits may be simplified to the point where a mathematical solution is not too arduous. It is believed that the method outlined below will give results with an error of less than 20 per cent, which is considered quite reasonable. Tests have shown that in some cases much greater accuracy can be expected. Only sufficient discussion is given to explain the method of calculation and for a more complete analysis of the problem reference should be made to the various papers listed in the bibliography.

Definitions and Basis of Calculation

The circuit recovery voltage is the voltage that appears across the contacts of a circuit-interrupting device immediately after it opens the circuit. Just before interruption the voltage across the contacts is limited to the arc voltage, and at the instant the arc is extinguished the voltage attempts to recover to the generated value. Since most short circuits are limited principally by reactance, in the usual case of a symmetrical current wave, the generated voltage is near its peak value when the circuit is interrupted at current zero and, therefore, the voltage recovery is from practically zero to peak voltage. This recovery is prevented from taking place instantaneously by the capacitance of the circuit and, because of the circuit characteristics, it may oscillate at high frequencies to nearly double its final value. Usually the capacitances involved are very small, and the recovery occurs in a matter of microseconds. For

Figure 1. Representative recovery-voltage circuit



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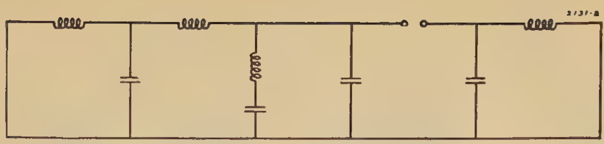


Figure 2. Simplified approximation of Figure 1

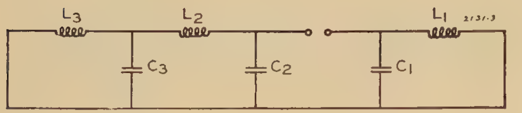


Figure 3. Simplified approximation of Figure 2

successful interruption the dielectric strength between the contacts of the interrupting device must exceed at all times the transient recovery voltage.

The method of calculation gives the *circuit* transient recovery-voltage characteristic and does not take into account any modifying action that the breaker may have on the characteristic. On an actual circuit the recovery-voltage characteristic may be modified by one or more of the following factors:

1. Asymmetry of the current wave.
2. Decay of flux in the generators during short circuit.
3. Arc voltage drop in the fault.
4. Arc voltage drop in the breaker.
5. Conduction current in the breaker after current zero.

These factors usually reduce the severity of the recovery-voltage characteristic. In order to compare calculated with test results, it is necessary to account for these factors when analyzing oscillograms obtained on test.

The calculation of the transient recovery-voltage characteristic involves the determination of the natural frequencies and voltages of the circuits affecting the recovery voltage from the inductances and capacitances of these circuits, and the combination of these values to obtain the voltage wave across the open breaker contacts. It is often not sufficient to obtain only rate of rise to the first peak, as this peak may be relatively low in comparison with some later peak which may be more important from the standpoint of breaker interrupting ability. Therefore, it is important to obtain a fairly complete voltage characteristic. The envelope over the wave is usually sufficient for this purpose.

Method of Calculation

The complete recovery-voltage circuit includes the inductance and capacitance of each piece of apparatus and of each line and connection. The mathematics is considerably simplified if distributed capacitances such as occur in transmission lines and cables are replaced by lumped values. Figure 1 shows a representative circuit resulting from this simplification. This circuit might correspond to that for a feeder breaker with a reactor on the feeder side of the breaker and a fault immediately beyond the reactor. Between the breaker and the feeder reactor is a capacitance to ground which is contributed by half of the breaker, half of the reactor, and the conductor between them.

On the bus side of the breaker is the capacitance of the bus and connected equipment. There are shown lumped reactances corresponding to generator, transformer, and feeder reactors. Transformers themselves are also represented here since they constitute substantially lumped reactances with a comparatively small amount of capacitance. Beyond these lumped reactances will be found the relatively large capacitances of generators, transmission lines, and cables, and, finally, completing the circuit, the additional reactances of the sources themselves.

Table I. Capacitance for Circuit 1

	Micromicrofarads
Reactor	100
Breaker and bushing	170
3 bus insulators—3×12	36
7′ of connections—7×7	49
Total capacitance	355

The circuit shown in Figure 1 is too complex for convenient treatment and so by paralleling reactances and capacitances in the source circuit and also in the outgoing feeder circuit, it is reduced to the circuit of Figure 2. This involves an approximation which would not be permissible in very precise work but will, in general, be satisfactory for the present purpose.

In a similar manner the circuit of Figure 2 is reduced to that of Figure 3.

It is possible to go immediately from the circuit of Figure 1 to the circuit of Figure 3 by means of the following procedure.

1. Determine the parallel reactance of all reactors connected to the bus except the one on the faulted line. Use the corresponding inductance as L_2.

2. Add together all the capacitances connected on the left side of these reactors. The resulting sum is C_3.

3. Determine the inductance corresponding to the reactance limiting short-circuit current on the bus. Subtract L_2 from this inductance. The result is L_3.

The transient corresponding to each of these circuits consists of a number of oscillations of different voltages, different frequencies, and different damping factors. Each of these oscillations considered by itself starts from zero and leaves a certain voltage across the breaker when it has died down, oscillating in the meantime between zero and twice that voltage value as shown in Figure 19. Figure 11 shows a transient in which a number of these oscillations are combined. The equation for such a transient is

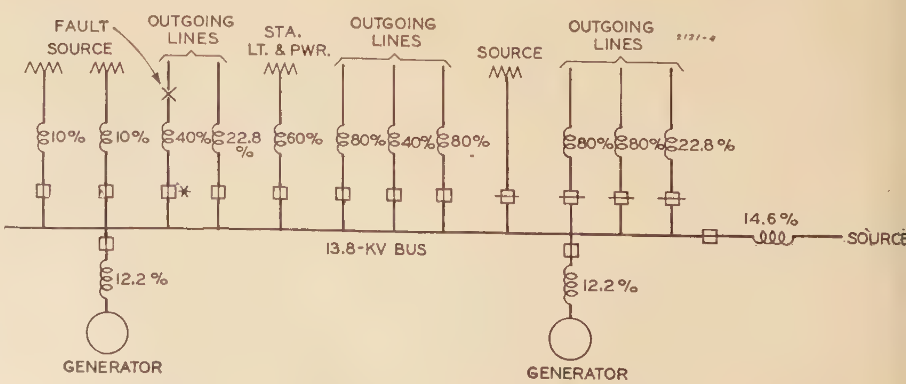
$$E = e_1(1-\epsilon^{-a_1 t}\cos 2\pi f_1 t) + e_2(1-\epsilon^{-a_2 t}\cos 2\pi f_2 t) + e_3(1-\epsilon^{-a_3 t}\cos 2\pi f_3 t) + \ldots$$

Table II. Capacitance of Circuit 3

		Microfarads
Leads to 2 transformers	2×0.584	=1.168
2 transformers	2×0.0048	=0.0096
2 generators	2×0.13	=0.26
2nd bus section (similar to first)		=0.02
4 lines		=4.0
		5.4576 (Use 5.45)

Figure 4. Station single-line diagram

*Breaker for which calculation is made



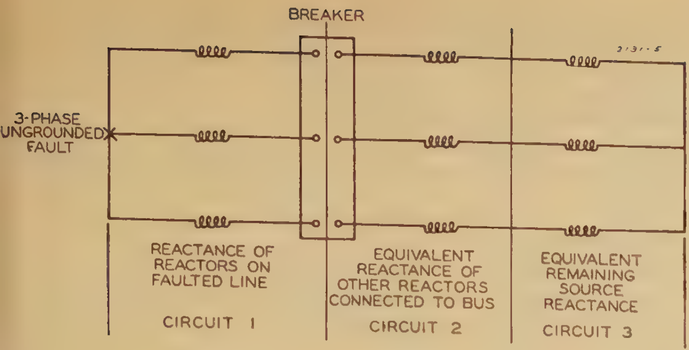


Figure 5. Reduction of system to three circuits

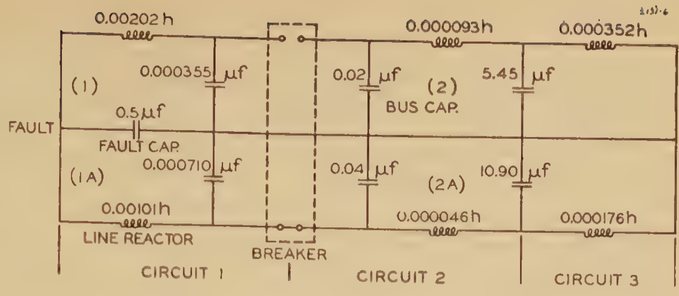


Figure 6. Equivalent circuit—three-phase

where e_1, e_2, e_3 . . . are the voltages associated with the various oscillations, f_1, f_2, f_3 . . . are the corresponding frequencies, and a_1, a_2, a_3 . . . are the respective decrement factors. Each of these oscillations may be considered to be associated with one inductance–capacitance pair (sometimes modified by the presence of the remainder of the circuit), and the voltages and frequencies are determined from the inductances and capacitances of these pairs.

Where an inductance and capacitance stand by themselves, as do L_1 and C_1 in Figure 3, the frequency associated with them is given by the well-known formula

$$f = \frac{1}{2\pi\sqrt{LC}}$$

and the voltage is equal to normal crest voltage times the ratio of the particular inductance concerned to the total inductance of the circuit. Where two adjacent circuits are coupled inductively or capacitively, however, they will react on each other, and this must be taken into account in determining frequencies and voltages. Boehne[3] has developed a method for doing this with two circuits so coupled.

Quite often with two adjacent circuits, the frequency of the first circuit is much higher than that of the second circuit, and the coupling capacitance is so high that it offers negligible impedance to current in the first circuit. In such a case fairly accurate results may be obtained by solving the two circuits independently. This is particularly helpful, where there are three or more adjacent circuits, in reducing the number to two so that Boehne's procedure can be applied.

Table III. Voltage Distribution

Circuit	Inductance	Per Cent Voltage	Volts
3A	0.000366 (L_R)	10.0	1,690
1A	0.000865 (L_S)	23.5	3,970
1	0.002020	54.5	9,210
3	0.000445	12.0	2,030
	0.003696	100.0	16,900

The decrement factors involve the resistances associated with the circuits. Since it is difficult to carry resistance values through these reductions, and fairly wide variation in decrement factor introduces comparatively small variation in the initial part of the curve, an approximate value for the decrement factor can be used for all circuits. In the survey referred to above, a factor which gave a decrement to 85 per cent of the initial value at the end of the first half-cycle was used for each oscillation. This factor gives a decrement to 20 per cent of the initial value in five cycles. This was based on observation of the decrement indicated in a number of cathode-ray oscillograms taken on actual circuit interruptions. Figure 19 shows a wave with this decrement.

This method must be considered to be a very much simplified one which will give results with reasonable accuracy in a great many cases. While it would be more accurate to consider both positive- and zero-phase-sequence values for all circuits, this will introduce additional complication which does not seem to be warranted except in special cases. Caution should be exercised, particularly where a higher degree of accuracy is desired, in simplifying circuits by the method suggested to avoid introducing unnecessary errors.

Inductances for circuits and equipment can be obtained from the reactance data used for short-circuit calculations. Capacitance data can be obtained by direct measurement when the circuit can be isolated, and when measuring equipment is available. However, if this is impractical, sufficient accuracy can be obtained by summation of the capacitances of the various circuit elements. Capacitances of equipment can be obtained from the manufacturers, the capacitances of cables and wires can be determined from standard formulas given in most handbooks, and those of copper connections in cells can be estimated by graphical methods.[8] Some data are given in the appendix for the more important circuit elements. It is extremely important that the capacitances of the higher-frequency circuits be estimated accurately, especially when the voltages associated with them are a relatively high proportion of the total voltage.

The following example illustrates the method of calculation. While there are various methods of simplification for circuits, only a few possibilities are illustrated. Various factors are brought out in the discussion of this example which have not been covered in the foregoing.

Example

This calculation obtains the transient recovery-voltage characteristic for the first phase of a breaker to open a three-phase ungrounded fault on a generating station feeder. The fault current is limited by reactors installed close to the breaker, with the result that the capacitance is low between the breaker and reactor, and the rate of rise of the transient recovery voltage for this portion of the circuit is extremely high. It will usually be found that circuits of this type give severe recovery-voltage characteristics.

The single line diagram of the station is shown in Figure 4 with breaker positions indicated and reactance values given on a

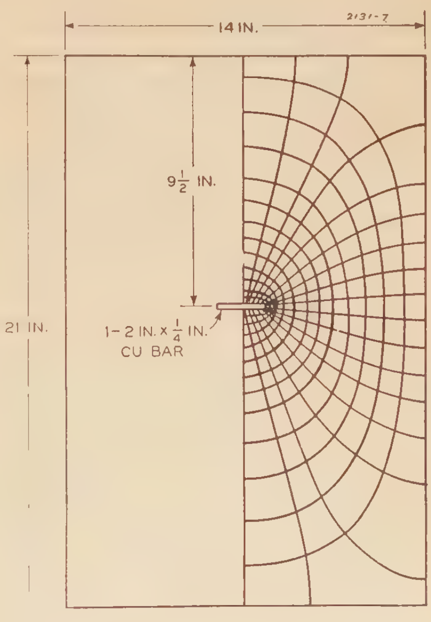


Figure 7. Flux plot for lead run in switch house

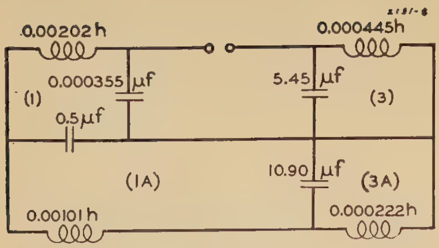


Figure 8. Equivalent circuit—reduced

100,000-kva 13,800-volt three-phase basis. In general, more severe recovery conditions are obtained when some circuits are disconnected, because this reduces the capacitance of the system. In analyzing the recovery characteristics of a system, it is advisable to consider probable operating connections for both normal and emergency conditions and to calculate the recovery characteristics for the one most likely to produce severe characteristics. This is analogous to the determination of short-circuit duty in applying circuit breakers.

The system can be reduced to three principal circuits limiting the fault current, Figure 5. These three circuits consist of:

1. A circuit from the fault to the breaker.
2. An equivalent circuit, the reactance of which is the parallel reactance of all reactors connected to the bus except the reactor on the faulted line.
3. An equivalent circuit representing the remainder of the system reactance.

The fault has been assumed to be a three-phase ungrounded short circuit, and the recovery characteristic is calculated for the first phase to clear, since this combination gives the most severe recovery-voltage characteristic. If it were considered impossible to obtain this type of fault, and, for example, only a phase to ground fault could occur, then the problem would reduce to a consideration of one phase only.

Phase-to-neutral capacitances are used for the three-phase ungrounded-neutral fault case. For most apparatus and for enclosed busses and connections these will usually be the same as phase-to-ground values.

At the time of clearing of the first pole, the second and third poles of the breaker are substantially closed circuits, and the circuit for the corresponding transient will be an outgoing circuit, consisting of the phase of the first pole to clear, in series with a return circuit consisting of the other two phases in parallel. Thus the return circuit will have just half the inductance and twice the capacitance of the outgoing circuit.

Introducing capacitances into the circuit of Figure 5 and paralleling the second and third phases, the circuit becomes that shown in Figure 6. The values of the constants are determined by the following method.

Circuit 1

The inductance will be that of the reactor in the faulted line. On the 100,000-kva 13,800-volt three-phase base, the reactance is 40 per cent, which is equivalent to an inductance of 0.00202 henry.

The capacitance will be that of the connections between the breaker and the reactor, including one end of the reactor and one half of the breaker. The capacitances of equipment are obtained from the tables in the appendix.

The capacitance of the connection was determined by means of the flux plot, Figure 7. This is a scale drawing of the conductor and the inside of the concrete cell with lines of flux and equipotential surfaces drawn in. The capacitance is calculated from the following equation:

$$C=2.7\frac{w}{l}\text{ micromicrofarads per foot}$$

where

w = number of tubes of flux = 34
l = number of cells per tube = 13

$$C=2.7\frac{34}{13}=7.06\text{ micro-microfarads per foot}$$

The total capacitance for circuit 1 is obtained by summation of the capacitances of the various parts, as shown in Table I.

Circuit 2

The reactance is equivalent to the parallel reactance of all reactors connected to the bus, except those on the faulted line. This parallel reactance is 1.83 per cent, equivalent to an inductance of 0.000093 henry.

The capacitance includes that of one half of the breaker on the faulted line, the connections from this breaker to the bus, the bus, and the connections from the bus to and including one half of the reactors. This is obtained by the method used for circuit 1 and is 0.02 microfarad.

Circuit 3

The inductance of the equivalent source reactance can be obtained by determining the equivalent system reactance for a bus short circuit and subtracting the reactance of the equivalent reactor of circuit 2.

From a short-circuit board study the bus fault current was determined to be 47,400 amperes, which is equivalent to a reactance of 8.8 per cent on a 100,000-kva 13,800-volt three-phase base. Subtracting the equivalent reactance of circuit 2

$$8.8-1.83=6.97\text{ per cent}$$

which is equivalent to an inductance of 0.000352 henry.

The capacitance of this circuit is considered to be that from the reactors to the next large lumped reactance such as a transformer or generator. Actually in the case of a transformer there will be additional circuits beyond the transformer, but for practical purposes these usually need not be analyzed separately.

In this case the leads to the transformer banks consist of two 2,000,000 circular-mil lead-covered cables per phase which have a total capacitance of 0.584 microfarad.

The transformers are General Electric Company rated 20,000-kva 13,800–69,000-volt three-phase, and are delta-connected on the low-voltage side. From Figure 13, and the method explained in the appendix, the capacitance is 4,800 micro-microfarads.

The generators are General Electric Company, rated 75,000-kva 13,800-volt three-phase. Substituting in the equation given in the appendix:

$$C=0.0187\frac{75}{\sqrt{13.8(1+0.08\times13.8)}}$$
$$=0.26\text{ microfarad per phase}$$

One half of this capacitance is considered concentrated at the generator terminals.

The lines are 3×350,000 circular-mil cable with an average length of two miles and a capacitance of 0.5 microfarad per mile. Since the lines are short, the total capacitance is effective, which for the

Table IV. Times to First Peaks

Circuit	Microseconds
3A	162
1A	67
1	2.6
3	155

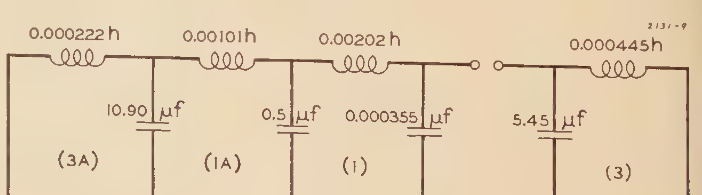


Figure 9. Equivalent circuit—rearranged

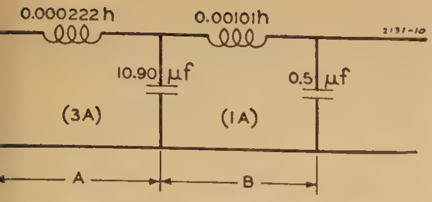


Figure 10. Circuits 1*A* and 3*A*

four lines is four microfarads. The capacitance of the station light and power section is negligible in comparison with that of the cables.

The total source capacitance is shown in Table II.

Fault Capacitance

Since the fault is ungrounded, there is a capacitance to ground at this point which consists of the three conductors of the open circuited line. Assuming the fault to be on a short line of 3,500 feet of 3× 350,000 circular-mil cable, the equivalent length of line to use for obtaining the fault capacitance can be obtained from the following equation which is explained in the appendix:

$$M=\left(\frac{\pi}{4}\right)^2 \frac{L}{l}$$

where

L = inductance of the return circuit = 0.001232 henry

l = 1/3 zero-phase-sequence inductance per mile of line in henries = 0.00067 henry

therefore

$$M=\left(\frac{\pi}{4}\right)^2 \times \frac{0.001232}{0.00067}=1.14 \text{ miles}$$

Since M is greater than the actual length of line, the total capacitance to ground of the line is used. For 3×350,000 circular-mil cable the zero-phase-sequence capacitance is approximately one half the positive-phase-sequence capacitance,[9] or 0.25 microfarad per mile. The fault capacitance is three times the zero-phase-sequence value, or 0.5 microfarad for 3,500 feet of cable.

Equivalent-Circuit Reduction

The upper part of the equivalent circuit, Figure 6, represents the first phase of the breaker to open, and the lower part represents the other two phases in parallel. The voltage will be distributed around the circuit approximately in proportion to the inductances, and the frequencies will be determined by the inductances and capacitances.

This diagram is simplified as explained below. Some of the reductions are approximations, but it is believed that they are sufficiently accurate for practical purposes.

Table V. Recovery Voltage

Time Micro-seconds	Circuit 3A 1,690 Volts π	Circuit 3A 1,690 Volts V	Circuit 1A 3,970 Volts π	Circuit 1A 3,970 Volts V	Circuit 1 9,210 Volts π	Circuit 1 9,210 Volts V	Circuit 3 2,030 Volts π	Circuit 3 2,030 Volts V	e Volts
2.6	—	—	0.04	—	1.0	17,000	—	—	17,000
67	0.41	1,250	1.00	7,350	25.8	9,210	0.43	1,620	19,430
100	0.62	2,230	1.50	3,970	38.4	9,210	0.65	2,840	18,250
120	0.74	2,700	1.80	1,550	46.0	9,210	0.77	3,370	16,830
140	0.86	3,000	2.10	1,270	54.0	9,210	0.90	3,700	17,180
155	0.96	3,120	2.32	2,460	59.5	9,210	1.00	3,760	18,550
162	1.00	3,120	2.42	3,260	62.3	9,210	1.04	3,740	19,530
180	1.11	3,000	2.69	5,400	69.1	9,210	1.16	3,500	21,110
190	1.17	2,870	2.84	6,120	73.0	9,210	1.23	3,280	21,480

Since the voltage across circuit 2 will be small compared with that of circuits 1 and 3, the error in adding the inductance of this circuit to that of circuit 3 will be negligible. Also, the error in neglecting the capacitance of circuit 2 and the capacitance of the two paralleled phases of circuit 1 will be small. By making these changes the circuit reduces to that shown in Figure 8. Figure 9 shows this circuit rearranged in a more convenient form for analysis. Since circuit 3 is separated from the rest of the circuits by the open breaker contacts there will be no interaction between it and the other circuits, and, therefore, its frequency will be determined directly by its inductance and capacitance.

By inspection of circuits 1 and 1*A*, it is evident that the frequency of circuit 1 is high and also that the 0.5-microfarad capacitance of circuit 1*A* is large compared with the constants of circuit 1. Therefore, circuit 1 can be treated as an independent circuit. This may be proved by the following method if desired.

For two interacting circuits with similar characteristics, the voltages and frequencies can be obtained by the method developed by Boehne.[3] Applying this method to circuits 1*A* and 3*A*, shown separately in Figure 10:

$$n_0=\frac{L_B}{L_A}=\frac{0.00101}{0.000222}=4.55$$

$$m_0=\frac{C_B}{C_A}=\frac{0.5}{10.90}=0.046$$

From Figure 20

$K_S=2.3$

$$f_A=\frac{1}{2\pi\sqrt{0.000222\times 10.9\times 10^{-6}}}= 3{,}240 \text{ cycles per sec}$$

$$f_B=\frac{1}{2\pi\sqrt{0.00101\times 0.5\times 10^{-6}}}= 7{,}100 \text{ cycles per sec}$$

$f_S=2.3\times 3{,}240=7{,}450$ cycles per sec

$f_R=\dfrac{7{,}100}{2.3}=3{,}100$ cycles per sec

The true frequencies (f_S and f_R) for these circuits, therefore, differ five per cent from the individual circuit frequencies (f_A and f_B).

Determining the voltage distribution by Boehne's method, using the magnitude curves, Figure 21:

$J_S=3.9$
$J_R=1.65$

As a check on these values, J_S+J_R should equal n_0+1

$J_S+J_R=5.55$
$n_0+1=5.55$
$L_R=1.65\times 0.000222=0.000366$ henry
$L_S=3.9\times 0.000222=0.000865$ henry

These new inductances are 30 per cent and 70 per cent, respectively, of the total inductance of the two circuits compared with 17 per cent and 83 per cent for the original inductances.

These new inductances are used to

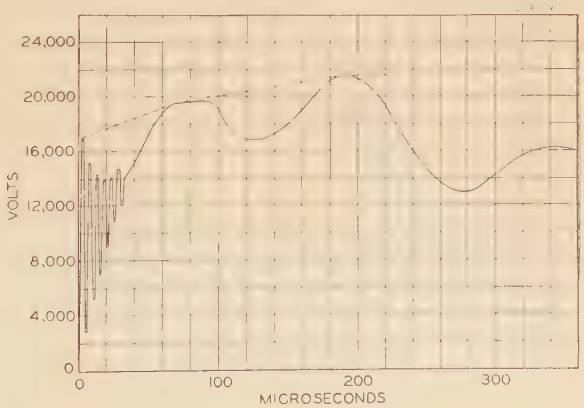


Figure 11. Recovery-voltage curve

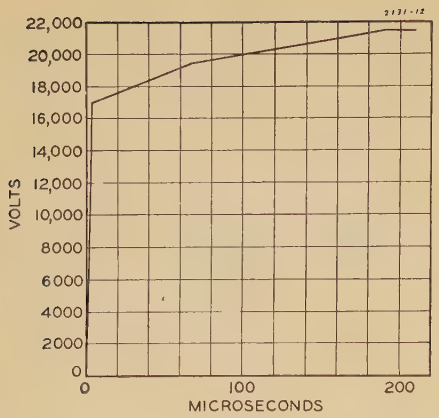


Figure 12. Envelope of recovery-voltage curve

determine the voltage distribution for circuits 1*A* and 3*A*.

Frequencies

The circuit frequencies are determined as follows:

Circuit 3*A*—3,100 cycles per second, from f_R above
Circuit 1*A*—7,450 cycles per second, from f_S above
Circuit 1

$$\frac{1}{2\pi\sqrt{0.00202\times0.000335\times10^{-6}}}=194{,}000 \text{ cycles per sec}$$

Circuit 3

$$\frac{1}{2\pi\sqrt{0.000445\times5.45\times10^{-6}}}=3{,}240 \text{ cycles per sec}$$

Voltage Distribution

The total voltage around the circuit for a three-phase ungrounded fault will be 1.5 times the phase-to-neutral voltage, that is, the crest value of the voltage of one phase plus the crest value of the mean of the voltages of the other two phases. For the system voltage of 13,800 volts:

$$e=13{,}800\frac{\sqrt{2}}{\sqrt{3}}1.5=16{,}900 \text{ volts}$$

The voltage will be distributed in proportion to the inductances as shown in Table III.

Recovery-Voltage Equation

Substituting the voltages and frequencies in the recovery-voltage equation gives the following for the transient recovery voltage of the circuit:

$$\begin{aligned}e=\,&1{,}690(1-\epsilon^{-a_1t}\cos 2\pi 3{,}100t)+\\&3{,}970(1-\epsilon^{-a_2t}\cos 2\pi 7{,}450t)+\\&9{,}210(1-\epsilon^{-a_3t}\cos 2\pi 194{,}000t)+\\&2{,}030(1-\epsilon^{-a_4t}\cos 2\pi 3{,}240t)\end{aligned}$$

First Peaks

The first peaks of each of these waves occur when

$$2\pi ft=\pi$$

therefore

$$t=\frac{10^6}{2f} \text{ microseconds}$$

For these circuits, the times to first peaks are shown in Table IV.

Recovery-Voltage Curve

The method for the determination of points on the recovery-voltage curve is illustrated in Table V. The values under the heading π are time expressed as a fraction of time to the first peak of the corresponding wave. The corresponding voltages are obtained by using these fractions as abscissae for the decrement curve, Figure 19, and multiplying the corresponding ordinates by the voltage for the circuit as given in Table III. The total voltage at any time is the sum of the voltages of the individual circuits.

The recovery-voltage curve out to the maximum peak is shown on Figure 11. The labor involved in determining this entire curve is considerable, however, and most practical purposes will be served if the envelope consisting of the curve up to the first peak and the dotted line joining the peak with subsequent peaks are shown. This requires the calculation of only a comparatively small number of points as shown in the table. The curve is shown in Figure 12.

The rate of rise of the recovery voltage may be considered to be the rate of rise to the first peak. For this example it is:

$$rr=\frac{17{,}000}{2.6}=6{,}500 \text{ volts per microsecond}$$

Table VI. Total Capacitance to Ground of Single-Phase Core-Type Transformers

Westinghouse Electric and Manufacturing Company

Kva	Capacitance in Micromicrofarads 115 Kv	161 Kv	230 Kv
5,000	2,550	2,750	2,350
10,000	2,450	3,650	2,750
15,000	3,400	3,650	2,950
20,000	3,650	3,650	3,050

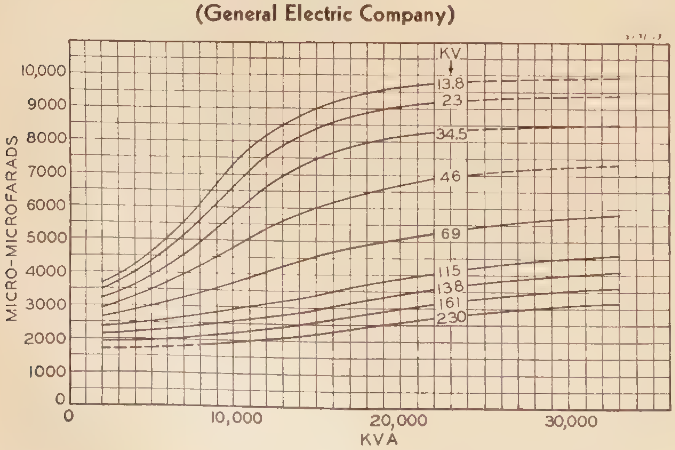
The maximum rate of rise is the slope of the line passing through zero and tangent to the first peak. This is:

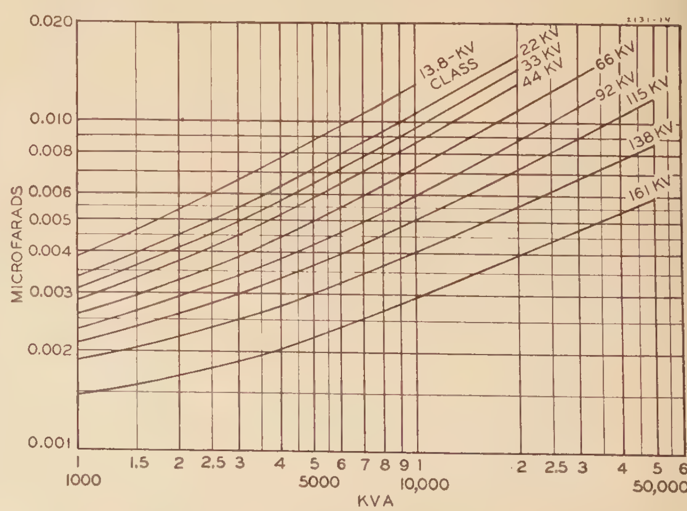
$$rr_{max}=1.14\times6{,}500=7{,}400 \text{ volts per microsecond}$$

As mentioned previously, the representation and simplification of this circuit exemplifies only one of a number of general methods. The representation of distributed constants by lumped constants sometimes requires more elements than at others, depending upon the circuit in which they are located. As the accuracy of the calculation depends upon the accuracy with which the circuit is repre-

Figure 14. Capacitance of typical power transformers—high-voltage to low-voltage and ground, single-phase, 60 cycles—data from tests (Allis-Chalmers Manufacturing Company)

Figure 13. Approximate trend of capacitance to ground of windings of single-phase core-type transformers with concentric windings (General Electric Company)





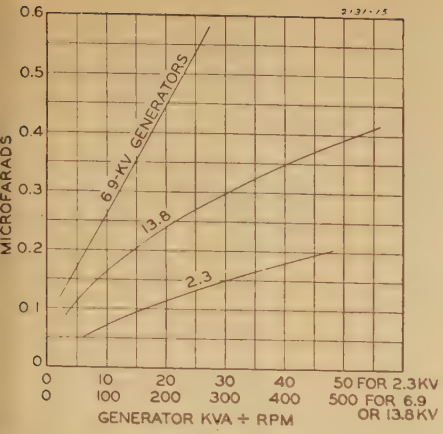


Figure 15. Effective capacitance to ground of salient pole generator stator windings—one-half capacitance of one phase to ground—all curves are the average of a large number of calculated curves (Westinghouse Electric and Manufacturing Company)

sented, as well as upon the assumptions made in simplifying it, both should be carefully studied.

Appendix. Capacitance Data

The following capacitance data are based on tests or computation by the manufacturer of the equipment unless otherwise noted.

Transformer Windings

General Electric Company. For single-phase transformers with concentric windings the total winding capacitance is given in Figure 13. These curves show only approximate trends. Individual transformers may deviate considerably from them. For capacitance of windings rated less than 13.8 kv, the 13.8-kv curve will apply.

The effective capacitance concentrated at each end of the transformer winding is one half of the curve value. The capacitance should be determined using the line voltage, whether the transformers are connected wye or delta, since the capacitance depends on the insulation thickness which is determined by the operating voltage of the transformer.

For a bank of transformers connected wye, one half of the capacitance of the transformer winding, as determined from the curve, should be used when considering each phase of the system. For a bank of transformers connected delta, the capacitance per phase is equal to the value obtained from the curve, because there are two transformers connected to each line.

Table VII. Total Capacitance to Ground of Single-Phase Shell-Type Transformers

Westinghouse Electric and Manufacturing Company

Kva	Capacitance in Micromicrofarads: 115 Kv	230 Kv
10,000	5,500	5,900
20,000	7,100	7,700

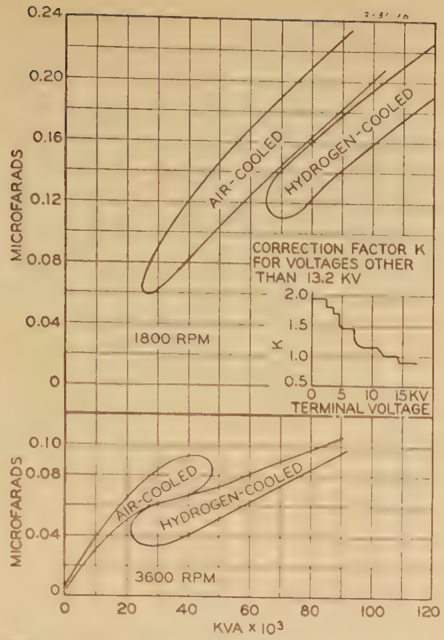


Figure 16. Effective capacitance to ground of turbine-generator windings—one-half capacitance of one phase to ground (Westinghouse Electric and Manufacturing Company)

Microfarad basis—13.2 kv; multiply capacitance by K for voltages other than 13.2 kv

For three-phase transformers, the capacitance per phase is approximately one half of the value given by the curve. The transformer kilovolt-ampere capacity to apply to the curves to obtain the capacitance is the three-phase kilovolt-ampere rating of the transformer.

For Pyranol transformers, the capacitance values are 1.6 to 1.7 times the curve values.

For a more complete discussion of these curves refer to "Equivalent Circuits of Transformers and Reactors to Switching Surges."[7]

Westinghouse Electric and Manufacturing Company. Table VI gives total capacitance to ground of single-phase core-type transformers. Table VII gives total capacitance to ground of single-phase shell-type transformers. The effective capacitance concentrated at each end of the transformer winding is assumed to be one half of the total capacitance.

Allis-Chalmers Manufacturing Company. The total winding capacitance for the high-voltage windings of various sizes of transformers are shown in Figure 14. The effective capacitance concentrated on each end of the winding is assumed to be one half the total value.

Table VIII. Effective Capacitance of Potential Transformers

Westinghouse Electric and Manufacturing Company

Kv Class	Ratio	Capacitance (Micro-microfarads)
25	23,000–115	180
34.5	34,500–115	175
46	46,000–115	185
69	69,000–115	310

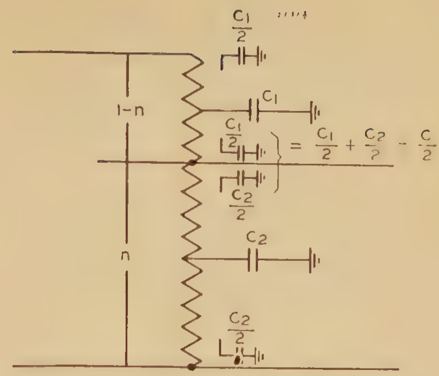


Figure 17. Capacitance distribution for autotransformers

Autotransformers

The capacitance of an autotransformer may be assumed to be distributed as shown in Figure 17.

C = total capacitance of autotransformer winding as determined from curve or tabulation (determine for transformer kilovolt-amperes, not circuit kilovolt-amperes)

n = ratio—low voltage to high voltage

$C_1 = (1-n)C$

$C_2 = nC$

Capacitance at high-voltage line end will be $C_1/2$

Capacitance at low-voltage end connected to tap will be $C/2$

Potential Transformers

Westinghouse Electric and Manufacturing Company. Effective values, that is, one half the measured winding capacitance, are shown in Table VIII.

Current Transformers

Average values for total winding capacitances are given in Table IX.

Induction Regulators

To determine the effective capacitance of induction regulators, it may be assumed that one half of each total winding capacitance is concentrated at each end of the winding as indicated in Figure 18.

C_P = total capacitance of primary winding
C_S = total capacitance of secondary winding

Table IX. Total Capacitance of Current Transformers

Kv Class	Type	Capacitance Micro-microfarads
General Electric Company		
7.5–15	Indoor	120
15	Outdoor	230
Westinghouse Electric and Manufacturing Company		
115	Oil-immersed Self-cooled	433

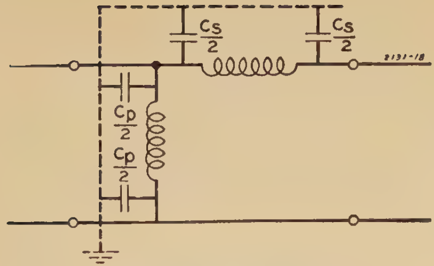


Figure 18. Capacitance distribution for induction regulators

Thus the effective capacitance at the source end is equal to $(C_P+C_S)/2$ while the effective capacitance at the load end is $C_S/2$.

Effective values determined from test readings obtained from regulators on the Philadelphia Electric Company system are shown in Table X.

Current-Limiting Reactors

An average value for the effective capacitance at each line terminal is approximately 100 micromicrofarads in the usual cell structure regardless of rating.

Generator Windings

General Electric Company. The following equation for the total capacitance per phase for generator windings has been taken from Park and Skeats' paper:[2]

$$C=K_c\frac{\text{mva}}{\sqrt{\text{kv}(1+0.08\,\text{kv})}}\ \text{microfarads}$$

where the constant K_c has the following values:

Solid round rotors	0.0187
Salient-pole generators without amortisseurs	0.0347
Salient-pole generators with amortisseurs	0.0317

and

mva = rated capacity of generator megavolt-amperes

kv = rated line-to-line voltage of generator in kilovolts

One half of the value obtained from the equation should be considered concentrated at each end of the generator winding.

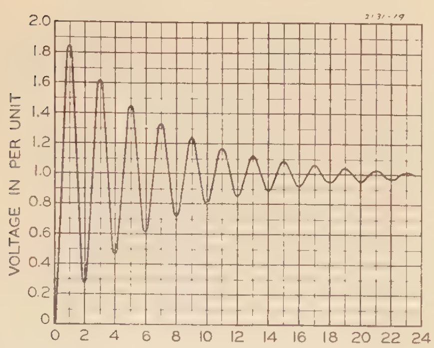


Figure 19. Curve for determining $1-\epsilon^{-at}\cos\omega t$ for decrement to 20 per cent in five cycles

$$1-\epsilon^{-0.051\omega t}\cos\omega t$$

$$\pi \text{ in microseconds for any frequency}=\frac{10^6}{2f}$$

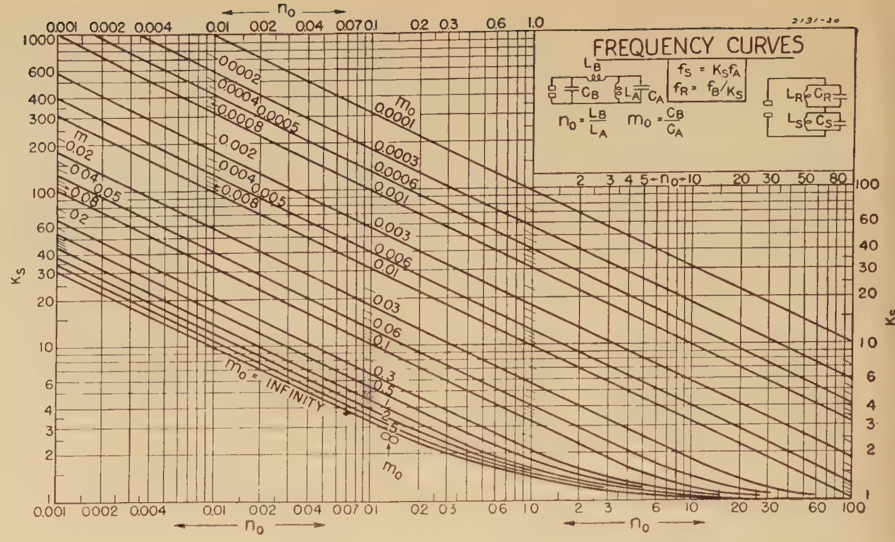


Figure 20. Frequency curves

Westinghouse Electric and Manufacturing Company. The effective capacitance per phase (one half the total capacitance per phase) is given in Figure 15 for salient pole generators, and in Figure 16 for turbine generators.

Oil Current Breakers and Bushings

The capacitance of an oil circuit breaker consists principally of the bushing capacitances. The values given in Table XI are for one bushing or for one side of an open breaker and should be multiplied by two where the circuit passes through the breaker.

The capacitance of General Electric Pyranol-filled bushings is approximately the same as that of similar oil-filled bushings.

General Electric type *H* breakers—15-kv (including bushings)

One tank to ground	125 micromicrofarads
Both tanks to ground	150 micromicrofarads

Insulators

Suspension-Type

Standard duty (Westinghouse)	23 micromicrofarads
Higher strength (Westinghouse)	25–28 micromicrofarads

Locke Insulators

Rating and capacitance of Locke insulators are shown in Table XII.

Bus Supports and Wall Bushings

Indoor bus supports	12–25 micromicrofarads
Wall bushings	100–200 micromicrofarads

Lightning Arresters

General Electric Company. Approximately 20 micromicrofarads. However, this is subject to considerable variation depending upon design and voltage rating.

Determination of Capacitance by Flux Plots

In order to determine the capacitance of a bus or of a conductor by this method,[8] a cross section of the conductor in the cell should be drawn to scale. If there are any grounded parts running through the cell, the flux path will be from the conductor to these parts. However, if the conductor is surrounded by a concrete cell, the walls of the cell can be assumed as ground potential, and the capacitance calculated on this basis.

In the space between the conductor and the grounded parts or cell wall, draw equipotential surfaces and draw lines of flux

Figure 21. Magnitude curves

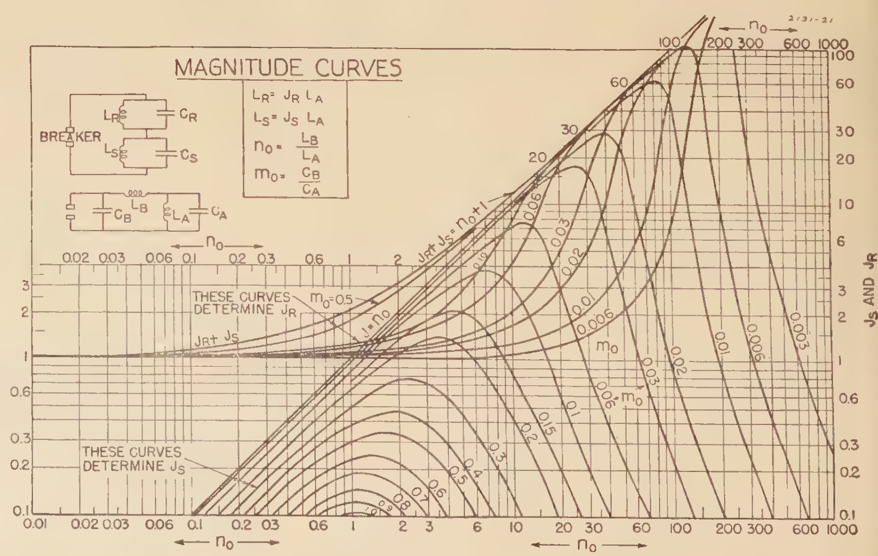


Table X. Effective Capacitance of Induction Regulators

Regulator Rating and Make	Effective Capacitance: Source End Micro-micro-farads	Effective Capacitance: Load End Micro-micro-farads
75-kva, 300/600-ampere, 2,500-volt, single-phase General Electric	6,250	2,250
72-kva, 300/600-ampere, 2,400-volt, single-phase Westinghouse	8,750	5,500
48-kva, 200/400-ampere, 2,400-volt, single-phase Westinghouse	4,750	2,500
34.5-kva, 150/300-ampere, 2,300-volt, single-phase General Electric	3,600	1,600
36-kva, 150-ampere, 2,400-volt, single-phase Westinghouse	3,300	1,500

from the conductor to ground. These should be so drawn that the lines of force and equipotential surfaces cross at right angles, and so that the cells formed are approximately squares. The lines of force should leave the conductor and enter the grounded surface at right angles.

The capacitance of the section can then be determined by counting the number of tubes of flux between the conductor and ground and the number of cells in series in each tube and by calculating the capacitance from the following formula:

$$C=2.7\frac{w}{l}$$ micromicrofarads per foot of length of conductor at right angles to the paper where

w = the number of tubes of flux
l = the number of cells in series per tube

Fault Capacitance

The capacitance for a three-phase ungrounded fault consists of the capacitance to ground of the three conductors and of any equipment adjacent to the fault. Usually the capacitance of the cable or line will predominate and that of the apparatus will be negligible in comparison.

Any line or cable may be assumed, with little error, to be open at its far end. If such a line is short, it will behave substantially as if all its capacitance were lumped at the fault. If the line is long, on the other hand, it will behave for a considerable time like a resistance rather than a capacitance. Nevertheless, in the interest of simplicity, it is very desirable to represent the line as a capacitance; but if the entire capacitance of a long line is used, the resulting calculation gives an initial rate of rise which is much too low, and accuracy in this component is more important in the early part than later on. Consequently, an approximation has been adopted which consists in placing a limit on

Table XI. Effective Capacitance of Oil Circuit Breakers

Breaker or Bushing Rating: Kv	Amperes	General Electric Company*: Without Capacitance Tap Micro-micro-farads	General Electric Company*: With Capacitance Tap Micro-micro-farads	Westinghouse Electric and Manufacturing Company† Micro-micro-farads
15	600	196		
	1,200	196		
	2,000	315		
	3,000	380		
25	600	160		
	1,200	160		100–300
	2,000	270		150–425
	3,000	335		250–500
34.5	600	150		300–700
	1,200	150		
	2,000			
	3,000			
46	600	145		
	1,200	145		
69	600	126	230	150–275
	1,200	126	230	
92	600	163	280	
	1,200	163	280	
115	600	163	294	250–315
	1,200	163	294	
138	600	160	306	300–375
	1,200	160	306	
161	1,200	155	310	340–435
196	1,200	235	390	
230	1,200	240	400	425–525
287	1,200			400–500

* Values apply to present standard bushings but will serve as a reasonably accurate approximation for bushings which are no longer standard. The former standard 400- and 800-ampere bushings have the same capacitance as the 1,200-ampere bushings listed here.

† Capacitance increases with rated continuous current and rated interrupting capacity.

the length of line taken into consideration. This limit is set at a value such that the first reflection from the far end of the line, as limited, returns to the fault point after one-fourth cycle of the oscillation resulting from the corresponding value of capacitance in combination with the inductance of the return circuit.

This limits the error in the first part of the transient to a maximum of about seven per cent of the total circuit voltage. In general, this maximum error will not occur at the time of an important peak in the recovery transient, and hence the effect on the recovery rate from this source will usually be quite small. This limit in miles of line is given by the equation:

$$M=\left(\frac{\pi}{4}\right)^2\frac{L}{l}$$

where

M = limiting line length in miles

Table XII. Locke Insulators

Catalog No.	Rating Kv	Capacitance of 1 Unit Micro-microfarads
29,150	15	15
10,455	34.5	25
9,153	46	11
7,785	115 (3)*	56
9,473	7.5	61
23,070	15–23	60

* Three units of 7,785 are used for 115 kv. Above value is for one unit. Three units will have approximately one-half the capacitance of one unit

L = inductance of the return circuit in henrys
l = $^1/_3$ zero phase sequence inductance in henrys per mile

If M as calculated is equal to or greater than the actual length of the line, the actual length of the line should be used in determining the fault capacitance. If M is less than the actual length of the line, this value should be used for the length of the line in determining the fault capacitance.

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Electric Facilities and Operating Plan for the First Chicago Subway

CHARLES E. De LEUW
NONMEMBER AIEE

Synopsis: The $64,000,000 subway project now being completed will provide long-needed rapid-transit terminal facilities in the Chicago downtown district. The State Street subway will be completely equipped and in operation early in 1943 and will constitute a major contribution to the problem of handling additional traffic resulting from a decrease in the use of passenger automobiles.

This first subway route is five miles long and with fixed equipment will cost almost $34,000,000. The subways were built with track sections in tunnel—at low level. Platforms 500 feet long are provided at stations, except in the congested area where there is a continuous island platform 3,300 feet in length with access provided by mezzanine stations in each block.

All fixed equipment is being installed by the city. Despite the delay in securing equipment because of present scarcity of critical materials, the entire project will be built and equipped in a period of four years. This high-speed construction program required expeditious planning of locations for all electric facilities.

Power will be 600 volts direct-current. The 144-pound contact rail will be energized by existing substations and energy delivered by conventional positive and negative feeders, with contact rails reinforced with parallel feeders.

Adequate sectionalization of the entire system is provided largely by automatic operation. Centralized supervisory control of the all-relay type includes most modern accessories, such as an illuminated diagram board and remote-control dispatching equipment.

Train movements will be controlled throughout by a modern signal and electro-pneumatic interlocking system with automatic stops. Signals are spaced and timed for operation of 40 trains per hour on each track. Signals and other electric facilities, including lighting, pumps, fans, and escalators will have a-c power supply.

Fluorescent lamps will be utilized at all subway stations. Lighting intensities on mezzanine floors and station platforms will vary from six to eight foot-candles. Escalators are provided at all stations. At the downtown and heavier outside stations two four-foot escalators are provided operating at a speed of 90 lineal feet per minute.

The State Street subway will be operated initially by 455 steel cars now owned by the Chicago Rapid Transit Company. However, new equipment will be modern streamlined articulated units. The ultimate capacity of the State Street subway as thus equipped will be 80,000 passengers per hour.

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The Initial Subway Project

THE $64,000,000 subway now being completed, is the first step in a comprehensive plan for modernization and extension of all local transit in Chicago. The second step, now well under way, will be the consolidation of three separate and competing transit companies into a single unified corporation. This will result in economies in operation and the elimination of unnecessary and costly duplication of service.

The unification ordinance, granting an indeterminate franchise to the new unified company, has been passed, and virtually all of the preliminary steps taken, so that there now remains only the formal approval by the present security holders of the reorganization plan, the approval of the issuance of new securities by the Illinois Commerce Commission, and the final approval by the voters at a referendum to bring to its final culmination this objective which the city has sought for more than 25 years.

The third step will follow the granting of an indeterminate franchise to the new company and will consist of a thorough-going modernization of transit equipment, the substitution of trolley and motor busses for inefficient trolley-car service on about one third of the track mileage of the present surface-lines system, the extension of and improvement of service in intermediate and outlying areas of the city, not now adequately served with local transit, and the unification of all facilities through universal transfer between rapid transit and surface routes.

Function

An understanding of the function of the new Chicago subways requires a brief outline of the present local transit situation.

More than three quarters of the city's traffic is carried by the Chicago Surface Lines, operating a comprehensive network of street-car and, in more recent years, trolley-bus and motor-bus routes. During the last two decades, this surface transportation system has been supplemented by the operation of motor-bus routes, largely over the boulevards, by the Chicago Motor Coach Company, now carrying about seven per cent of the total traffic. The only form of urban rapid transit, other than that afforded by steam-railroad suburban roads, is that provided by the elevated railroad system operated by the Chicago Rapid Transit Company. However, unification of these companies will bring universal transfer and thus making rapid-transit facilities indirectly available to all.

For years engineers have realized the inadequacy of the downtown terminal facilities of this system. Thirteen elevated tracks, four on the north side, six on the west side, and three on the south side, deliver their loads to a two-track loop in the center of Chicago (except for a limited number of trains handled in stub terminals).

One of the principal objectives of the State Street subway is to provide terminal facilities for the rapid-transit lines to relieve the elevated loop. The loop, about two miles around, consists of two tracks both operating in the same direction. The cars using the inner loop must cross the outer-loop tracks at grade on both entering and leaving.

In spite of interference between all trains at two points, the outer loop now carries at the peak of service 208 cars in 35 trains during the maximum 30-minute period—see Figure 1. The inner loop now carries 167 cars in 36 trains. Commonly accepted practice to move trains expeditiously is a maximum of 40 trains per hour, and the heavily traveled New York subways operate at a lesser frequency than this. As a result of this congestion, speeds on the Chicago elevated loop lag down to six or seven miles per hour, and overcrowding of trains results—even under present traffic conditions.

The importance of additional terminal tracks for the Chicago rapid-transit system became vastly more important with the order rationing passenger automobile tires which became effective about the first of the year 1942. It has become evident that public transportation agencies will be required to carry, not only the additional traffic resulting from the accelerated pace of commerce and industry due to war activities, but also an ever increasing group of passengers who now use auto-

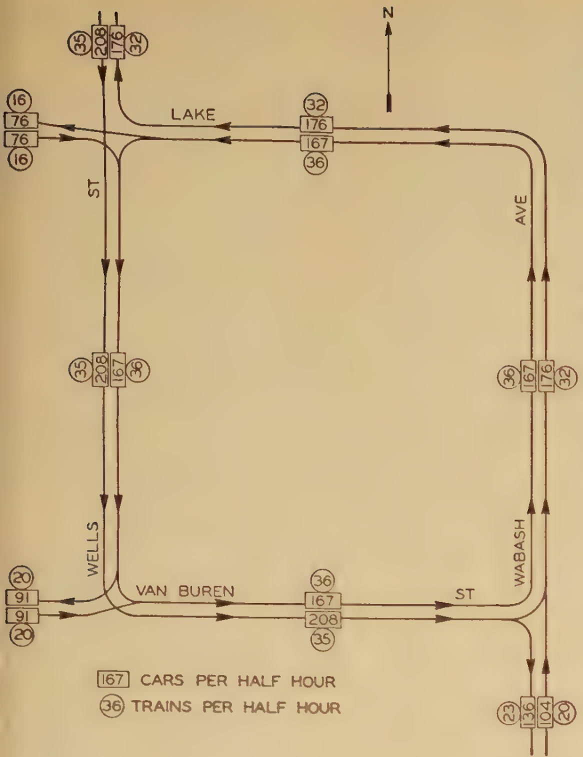
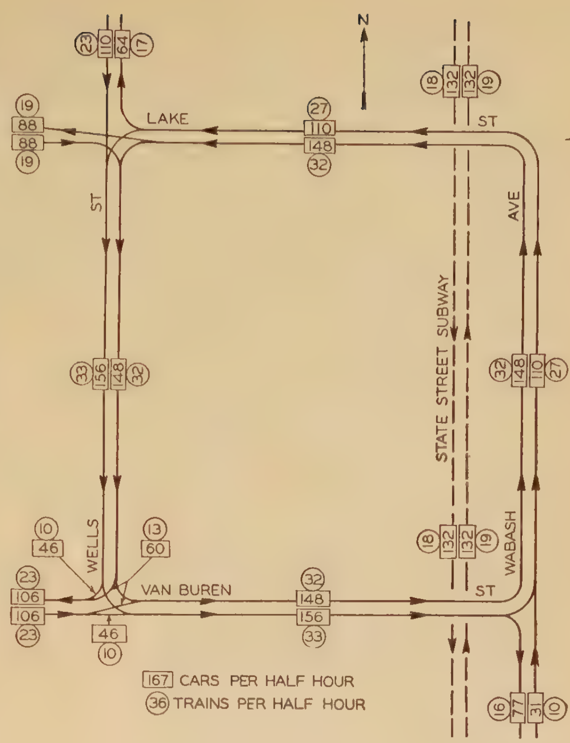


Figure 1 (left). Chicago rapid-transit car flow on the Union Loop during maximum half hour, 8:15–8:45 a.m., December 1941

Figure 2 (right). Car flow on Union Loop and State Street subway during maximum half hour, 8:15–8:45 a.m., expected in December 1942



mobiles exclusively for their transportation.

Because most automobile traffic to and from Chicago's downtown district is long haul traffic, it is estimated that most of the present automobile passenger load will be transferred to rapid-transit trains. Our estimates indicate that unless relief is afforded by the operation of the State Street subway during the year 1943, it will be virtually impossible to operate enough cars over the existing union loop terminal to provide transportation with any reasonable loading standard. Figure 2 shows the estimated redistribution of present traffic after the start of operation of the State Street subway.

Description of Subways

The initial project consists of two terminal subways which will be operated with and become an integral part of the rapid-transit system. Route 1, the State Street subway, connects with the four-track north-side elevated route near Armitage Avenue, $2^1/_2$ miles to the north, and extends along Clybourn Avenue, Division Street, and State Street to a connection with the three-track south-side elevated route near 16th Street, $1^1/_2$ miles to the south. This subway has a total length of 4.9 miles and is estimated to cost $33,750,000 including fixed equipment.

Route 2, the Dearborn Street subway, is the first unit of an underground terminal system for west-side rapid-transit routes. The initial section extends from a downtown terminal loop to a connection with the Logan Square elevated route, $3^1/_2$ miles to the northwest of the downtown district. The total length of route is 3.9 miles, and the total cost, including fixed equipment, will be approximately $30,400,000. Except for short sections near the terminals, the entire project was built with track sections in tunnel at low level. Reinforced concrete tunnel sections were utilized throughout.

At each station platforms 500 feet in length are provided. At the three side-platform stations these structures consist of multiple arches—largely of reinforced concrete construction. Stations with continuous island platforms 22 feet in width were built in the loop district proper. This construction consists of a multiple three-arch section—two large bores on each side accommodating the trains and a portion of the platforms, with a smaller arch sprung from longitudinal steel girders supported by heavy steel columns. In general, stations are of the mezzanine type, constructed by cut-and-cover methods and located as close as possible to the street surface. Figure 3 shows an artist's perspective of the subway structure at a station.

All architectural features were designed with a view to providing attractive, light, and well-ventilated stations. A type of finish is planned which, in addition to being attractive, will stand wear and tear as well as the rigorous atmospheric conditions of downtown Chicago.

As finally developed the plans for the mezzanine stations provide for reddish-colored cement floors, scored in a tile pattern, light-colored structural glass walls, painted concrete ceilings, with dark (radio-black) marble column encasement, and metal doors and trim. Stairways have abrasive tile treads and glazed tile walls. The platforms are also to have colored cement floors, scored, with various colored paints on metal columns and all exposed metal—also on the arched ceilings.

Construction

Electrical Problems

Since the subways were tunneled through the soft clay underlying most of the city, air pressure was used during mining operations, and one of the first electrical problems confronting the subway department was the regulation of the contractors' electric facilities, including the service transformers, motor-driven air compressors, temporary electric lighting and hoists. The specifications were written to provide safety to the men working under air pressure in the tunnels. To insure reliability the power company was required to provide two entirely separate feeders from separate substations to each plant, each supplying a separate bank of transformers. Transfer switches were required as well as high-class equipment and wire throughout. Tunnel lighting and air compressors were especially safeguarded against failure.

The design of the permanent electrical work in the structure had to be made far in

advance of installation, because the conduit requirements for the feeders and branch wiring, as well as the space requirements of the equipment, had to be known before the concrete was poured. This subway is virtually free from all exposed wiring and cable.

Splicing chambers, circuit-breaker rooms, and machinery rooms all form a part of the underground structure. The space requirements of these rooms, therefore, is an important factor in the cost, and the electrical department had to be able to justify all space asked for. Arrangements had to be made with the power company for duct connections and transformer vaults, where necessary, so that these could be installed before the excavation was closed. Therefore, the electrical studies were intensive, even while the mining operations were just beginning.

Equipment Installed by City

Under the provisions of the unification ordinance passed in 1940, the new transit company is obligated to install all equipment in city-built subways. However, by the spring of 1941 it had become apparent that the seemingly interminable delays incident to completing all preliminary legal and financial negotiations might result in the first subway being completed before the reorganization was effected.

Therefore, the City Council, in May 1941, by ordinance directed the Commissioner of Subways and Superhighways to purchase and install the fixed subway equipment and to make arrangements with the trustees of the Chicago Rapid Transit Company for subway operation. Subsequently, the department proceeded vigorously with this work with the understanding that the city will be subsequently reimbursed for all equipment expenditures by the new transit company. In this task our engineers have had the full co-operation of the engineering staff of the Chicago Rapid Transit Company.

Progress

Basic subway construction on route 1 is complete. The elevated-to-subway connections at termini are now under construction. Station finish, much of which was redesigned to reduce the quantity of critical war materials, is now 50 per cent complete.

Anticipating some difficulty in securing the large quantities of steel, copper, and rubber required for fixed subway equipment, orders were placed and contracts let for furnishing most of the necessary equipment late in the year 1941. The last such contract, for signals and interlocking, was placed during April 1942; subsequently, contracts have been awarded for the installation of all traction power distribution equipment—work which is now under way.

The War Production Board has recognized the importance of completing the State Street subway by assigning a project rating of A-10 in February 1942. Higher ratings have been assigned various suppliers and manufacturers as required, so that the completion of the entire State Street project, including equipment, ready for operation early in 1943, now seems assured.

Power System

Trains operating in the State Street subway will be routed through the subway in the central area from the present outlying elevated terminals. The power for the subway trains will therefore be the same as that used on the elevated system, that is, 600-volt direct-current on the contact rails with paralleling feeders and with a return to the substations through running rails, negative feeders, and, to a limited extent, through steel elevated structures.

Substations and Feeders

Because the State Street subway is generally parallel to the present north and south elevated route, and because the downtown terminal traffic will be transferred from the elevated system to the subway, the subway power requirements will not be entirely in addition to the present system power requirements. Therefore, it is practicable to use existing substations now supplying the transit system. In most cases, these substations are actually adjacent to the subway structure, although one, the Sedgwick Street substation, is several blocks distant. New feeders will connect substations to the subway.

The substations are owned and operated by the Commonwealth Edison Company and under present plans this company will supply traction power to the subway. In general, the only new substation equipment required for the subway will be feeder circuit breakers and their automatic and control features.

The underground positive and negative feeders from the substations to the subway cable shafts will be paper-insulated, lead-covered cable. They will be spliced to water-resisting compound insulated feeders leading from cable shafts to circuit-breaker rooms in the subway structures. All power feeders in the subway will be insulated with water-resisting compound and finished with heavy asbestos braid. The subway circuit-breaker rooms at feed points will contain circuit breakers which will act as remote-controlled disconnect switches between these substation feeders and contact-rail feeders.

Contact-Rail System

A new contact-rail section has been adopted, similar in design but considerably heavier than that now used on the elevated system. This rail weighs 144 pounds per yard and will have a conductivity equivalent to 2,250,000 circular mils of copper. The rail will have a hardness somewhat greater than has been used for high-conductivity rails on other subways, to obtain longer life.

Since the contact rails are not generally of sufficient conductivity for the subway loads, it is necessary to reinforce them with paralleling feeders. These reinforcing feeders will be insulated with water-resisting compound and tapped to the contact rails at intervals of approximately 1,000 feet through disconnecting tap switches. The negative reinforcing feeders, in parallel with the running rails, will also be insulated with water-resisting compound and will be tapped to the running rails at intervals of approximately 400 feet. The positive and negative reinforcing feeders will be carried in a duct bench containing 12 asbestos-cement conduits along one side of each track in the train sections. The duct bench also serves as an emergency walkway—see Figure 4. Splicing chambers are located at 250- to 400-foot intervals.

The taps to the contact rails and running rails will be cables similar to the reinforcing feeders and are carried in asbestos-cement conduits in the concrete track ballast to pothead terminals, from which flexible bare bonds will connect to the contact or running rails. The location and gauge of the contact rail will be the same as that of the contact rail on the elevated system, so that the same collecting shoes can be used. This type of contact shoe is of the suspended overrunning contact type. The contact rails will be mounted on porcelain insulators on the track ties in the usual manner. Expansion and contraction will be taken care of by gaps at intervals of approximately 1,000 feet on level tangent track in the subways and at shorter intervals on curves and grades.

Sectionalizing

The contact rails with their reinforcing feeders are sectionalized at the substation feed points, interlocking plants, the emergency track crossovers, and at the tie points which are approximately midway between feed points. Short-circuit and overload protection on any section between substations is obtained by automatic opening of substation circuit

Figure 3. Artist's perspective of the subway structure at a downtown station

The functions to be performed include remote control and supervision of all feeder, sectionalizing, and tie-station d-c circuit breakers, as well as the operation of the two-speed station and tunnel ventilating fans. Additional functions will include supervision of the continuity of the normal and emergency a-c services, high water level and pump-motor overload, low supervisory-battery voltage, battery ground or ground on control wires to apparatus remote from the control center.

The supervisory-control system is an "all-relay" system using relays of the standard telephone type, having uniform mechanical construction, extreme simplicity, and established dependability. Operation of this system is based on codes of

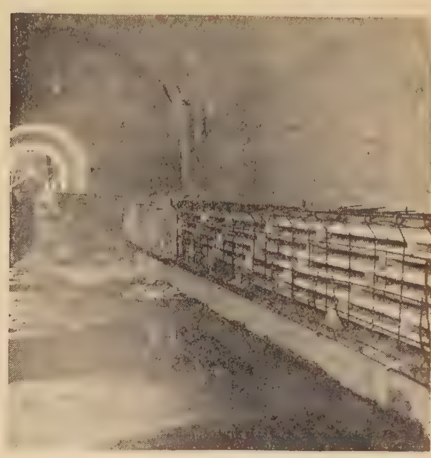


Figure 4. Duct bench and walkway during construction

breakers and tie circuit breakers without affecting other sections. The other sectionalizing circuit breakers do not have automatic opening features but serve as remote-controlled disconnect switches with high interrupting capacities. The function of the automatic circuit breakers at tie points is to connect the contact rails and feeders of the two tracks together in order to utilize the conductivity of both for feeding distant loads.

The tie station circuit breakers, the substation circuit breakers and all sectionalizing and disconnect circuit breakers will be of the latched-in, air-break type and will be remotely controlled by the supervisory control system.

Return Circuit and Bonding

Since the block signal system of the subway tracks will be of the single-rail track circuit type, one rail of each track will be used exclusively for signals, leaving only one rail of each track available for the traction current to return to substations. Each running rail will have conductivity equivalent to approximately 1,000,000 circular mils of copper. This conductivity must be supplemented by the negative reinforcing feeders. Running rail joints will be welded, except at special track work where bonds will be required across joints. Bonds will be bare flexible copper cable with terminals suitable for welding to the head of the rail by the oxyacetylene welding process.

The contact-rail joints will not be welded, so they must be bonded. The contact-rail bonds will be short U-shaped cable bonds located underneath the rails with terminals welded to the flanges. There will be two 750,000-circular-mil copper bonds per joint.

Supervisory Control

Due to the physical extent of this project and the diversity of operations associated with power services for a subway, including ventilating and pumping systems, centralized control and supervision is essential. This is to be provided by supervisory control of the all-relay type.

The dispatching equipment of the supervisory-control system will be located in the power supervisor's office, two blocks from the State Street subway. Remote station supervisory-control equipment will be located at eight control centers.

A power supervisor's desk, an illuminated diagram board, and relay cabinets of the swinging panel type will comprise the principal elements of the dispatching equipment. Although these elements will incorporate dispatching equipment for all eight outlying centers, the equipment will function independently for each. Thus operations of any given center will not interfere with simultaneous functions of another center.

impulses suggestive of a simple telegraph system, but different from the telegraph in that the impulses are all of the same time duration. Several impulses in uniform sequence form a code, and the number of impulses in a code is varied to obtain various results. All codes are produced automatically, the dispatcher's action being limited to the simple twisting of a control key and the pushing of a button.

As an example of operation, assume that it is desired to close a circuit breaker. The dispatcher first operates the selection key on the escutcheon of the desired breaker. This sets the proper relays into operation to select the breaker. As soon as this selection is made, the fact is indicated by the lighting of the white selection lamp on the associated escutcheon, as well as the lighting of a similar selection lamp on the diagram board. The supervisory equipment now comes to rest, awaiting the will of the dispatcher. Then, to close the breaker, the dispatcher first places the twist-type control key in the closed position and then operates

the master control key. This results in the closing of the breaker. When the breaker closes, the auxiliary signal contact closes and energizes supervision relays which operate to change the indications of the red and green supervision lamps on the diagram board. The supervisory control equipment then returns to the normal rest position, ready for the next operation or change in indication.

The system is operated entirely from 48-volt storage batteries to insure against loss of control from loss of a-c power. A check-back feature is employed as a guard against the possibility of obtaining false indications or performing false operations. Multiconductor control cable will provide the necessary channels for operation of the supervisory-control equipment. Each of the eight remote supervisory centers will be controlled and supervised over a single pair of wires. In addition to the pair per station, there will be two standby pairs. One standby pair will be available to the five stations to the north of the dispatcher's office, and the second pair will be available to the three stations to the south.

Throw-over switches at all points will facilitate transfer to the emergency lines in case of trouble. An audible and a visual alarm will be given in the power supervisor's office in case any of the control lines are opened, grounded, or short-circuited. In addition, each line will be equipped with supervisory-control protectors which will function to clear any heavy surges which may appear on the line wires.

Emergency Alarm System

Adjacent to splicing chambers located at intervals of about 400 feet along each subway track, emergency alarm boxes will be located on the walls above the walkways. These alarm boxes will be similar to city fire-alarm boxes in that when the lever on one of them is pulled, an alarm is given at the power supervisor's office. The alarm will be audible and visible, and the number of the box on which the lever is pulled will be recorded on a tape together with the time and date. Also, the pulling of an alarm-box lever will immediately open all circuit breakers through which power is normally supplied to the contact-rail section where the alarm box is located. Thus, in case of an emergency, such as a train wreck or fire, the motorman of an affected train, or other employee, can quickly de-energize the contact rail and at the same time give the alarm at the power supervisor's office. A telephone will be located by the side of each alarm box to enable the person actuating the alarm box to give further information and ask for and receive instructions concerning the emergency.

The alarm circuit will be a single-wire loop connecting all boxes in series. Each alarm box, when its lever is pulled, will cause a spring-driven contactor to open and close this loop circuit automatically at intervals in a code corresponding to the box number. If levers on two to four different boxes are pulled at the same or nearly the same time, each will transmit its code in turn, and all signals will be received at the supervisor's office without interference. The loop circuit will have automatic line supervision which will cause a distinctive alarm signal to be transmitted and recorded, in case of a ground or open circuit on the loop. The source of current for the alarm circuit and recorder will be a 48-volt storage battery at the power supervisor's office.

The circuit breaker tripping circuits will be an individual series loop for each contact-rail section. The opening of one of these loops will effect the de-energizing of the contact-rail section by means of relays, causing the supervisory-control tripping relays to trip the breakers. The alarm-system tripping relays are so designed that the power supervisor can reclose the circuit breakers by supervisory control after a short interval. The source of current for the alarm system's tripping loops will be the storage batteries at the outlying supervisory-control centers.

Signal System

The signal system consists of wayside colored-lights, approach interlocking signals, home signals, and interlocking dwarf signals. All of these except the dwarf signals have automatic controls; the approach and home signals are also controlled manually to govern train movements to and through interlocking plants. Dwarf signals (sometimes called backup signals) are manually controlled.

Automatic Train Stops

Associated with each automatic block, approach, and home signal there will be a wayside electropneumatic train-stop mechanism which in the tripping position would engage trip arms on the train thereby applying brakes and enforcing obedience to the STOP indication of the signal. A stop-release contactor mounted on the signal when operated by the motorman will clear the stop arm and permit him to pass a red signal when it is necessary to close up to the train ahead, or when signal or car-equipment failure occurs—according to the "stop, then proceed" rule.

Timing Control

The tracks are divided into blocks with a signal and train stop at the entrance to each block. The minimum length of a block is equal to the emergency braking distance for the expected maximum speed of a train as it enters the block, plus an additional length as a factor of safety. Minimum-length blocks are used in station timing where a train is allowed to close in at restricted speed when the station section is occupied. An illuminated T sign informs the motorman of the train where to begin this restricted speed. Signals governing entry into a station have extended controls so that the train standing in the station is protected by three or four red signals or by a distance sufficient to protect it from a following train traveling at maximum speed. The approaching train, on entering a track section or block where the T sign is located, energizes a time-element relay which picks up at a predetermined time and cuts off the extended control of the next signal, allowing it to clear for the train now traveling at a restricted speed. If this train continues at restricted speed, the next signal will have its extended control cut off by a second timing relay and so on, to bring the following train up closer to the station. This station timing is necessary to provide for the 90-second headway for which the signal system is designed. With no train standing in a station, all closing-in signals will be green, and a train can then proceed at normal speed.

On descending grades the braking distance is greater, and therefore the blocks at high speed would be so long as to reduce the track capacity. Restricting the speed of trains on grades results in increased safety and increased capacity. To accomplish this, "grade timing" is used with one or more signals normally showing the stop indication. If the trains run at excessive speed, a caution signal will be displayed, together with an additional lunar white indication, which indicates that the next signal is at "stop", only because the allowable speed is being exceeded, and that it will clear if the train runs at the permissible speed as measured by a timing relay.

These timing relays will receive coded energy for the measurement of the time intervals controlled by contacts on the track relays. The code transmitter, consisting of a coil, a mechanically tuned oscillator, and contacts, will generate the desired code for a number of time relays as determined by the location and distribu-

tion of these timing relays. Timing relays are also used for the emergency release of approach and route locking at interlocking plants. The time intervals will vary from four seconds for station timing to a maximum of 120 seconds for emergency switch operation.

Interlocking

At interlocking plants, switches and signals are controlled by means of buttons on a control board, and electric relay interlocking, in lieu of the mechanical locking which formerly was standard with interlocking machines. The control board is mounted on a desk within easy reach of the operator and has engraved on its face a miniature diagram of the tracks and switches of the interlocking plant, backed with lights repeating the track sections, thus showing the presence of a train as it approaches and moves through the plant. The interlocking control circuits are designed so that to initiate a route the operator need only manipulate a knob or button on the control board at the beginning of a route and then a second button at the end of said route. This operation causes the various switches and signals of the route to be correctly positioned and indicates on the control board that these switches have responded. This operation further electrically locks other switches and signals so that no conflicting move can be set up. Circuits are also provided for approach and sectional-release route locking which is standard in interlocking practice. Each switch and crossover has a separate operating lever on the control board for independent manipulation. An indication is also provided for each switch or crossover to show when they are not free to move, either because of the electric locking, or because a conflicting position of the switch has been called for.

The entrance control button has a red and a green lamp within it. The red lamp will flash when a route is initiated, will become a steady red light when the route has been completed, and will change to a steady green light when the signal has cleared.

Relays and Track Circuits

All relays are of the plug-in type with terminal prongs at the back. These prongs fit into sockets set into bases that are permanently mounted on racks. The control wires are soldered to these sockets. A maintainer desiring to replace a relay pulls one out of a socket and plugs in a duplicate without disturbing the wiring.

All interlocking and signal-control relays operate on ten volts direct current. Track relays are of the a-c vane type and operate over single-rail track circuits, where one rail is given over to signal track circuits, and the other rail is used both for power return and for track circuits. A track relay is connected across the signal and negative rails at the entrance to a block, while the approximately four-volt secondary winding of an air-cooled track transformer is connected across these rails at the far end of the block of track section. The relay and transformers are protected from unbalanced propulsion current by means of a resistance unit inserted in the leads to the signal rail and also by 600-volt fuses. The resistance unit in the transformer lead further limits the flow of current with a train in its track section. The track transformer has another secondary winding which furnishes ten-volt energy for lighting all signal lamps.

Figure 5 (left). Front of typical a-c switchboard

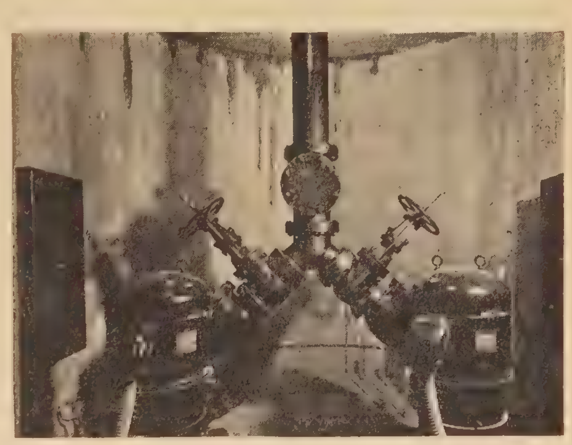


Figure 6 (right). Typical pump room

Signal Power Supply

The signal power-supply equipment is located in eight signal rooms in the State Street subway and at the north and south portal interlocking plants. Two liquid-cooled transformers at each location, one furnishing the normal supply and the other acting as a reserve source of power, receive energy at 60 cycles 208 volts in the subway and 240 volts at the two portals. The secondary circuits are interconnected by means of an automatic transfer switch and feed power to the a-c signal mains at 110 volts.

Two rectifiers of the copper-oxide type at each power-supply location are connected in parallel and feed power to the d-c signal mains at approximately 16 volts. These signal mains extend in each direction and in each tube from the power-supply source approximately half way to each adjacent supply location. The a-c signal mains feed the interlocking control-board lights, all signal lights, and track circuits. The d-c mains feed the relays at interlocking plants, all signal-line control relays, timing relays, and the electro-pneumatic valves.

Compressed Air

Air-compressor plants are located at the north-portal interlocking tower and at the 13th Street junction. Each compressor plant is equipped with two compressors connected to individual 600-volt d-c motors with automatic controls and fed from the propulsion current supply. The automatic controls are so arranged that either or both of the compressors may be cut in, depending on the drop in pressure. The compressed air is for operation of the automatic train stops and the track switches at interlocking plants.

The air distribution system consists of $1^1/_2$-inch air pipe extending the entire length of each subway tube and two-inch air mains on the elevated structure. Branch air lines feed compressed air at a normal pressure of approximately 65 pounds per square inch to the cylinders of the automatic stop and switch operating mechanisms.

Other Electrical Features

A-C Power Supply

The power for operating the a-c electric equipment will be supplied from frequent distribution centers along the subway by the Commonwealth Edison Com-

pany through 208/120-volt three-phase four-wire grounded neutral feeders. The total connected a-c load of the State Street subway and approaches (approximately ten track miles) is divided roughly as shown in Table I.

On this 4.9-mile two-track State Street route there are 21 a-c switchboard rooms in the subway, each supplied by duplicate service feeders from the Commonwealth Edison Company's transformer vaults. In the section outside of the central business district, the duplicate feeders are not merely parallel feeders; they come from normal and emergency sources, fed from different transformers. In the downtown area, the feeders come from transformer vault busses supplying the 208-volt three-phase network of the power company. These busses are interconnected with other vaults, and trouble in a vault supplying the subway would not interrupt services because of the network isolators. The reliability of the primary networks feeding transformers is well known.

Each a-c switchboard in the subway—see Figure 5—consists of a manually operated normal and an emergency service feeder main circuit breaker, on the load side of which is an electrically operated latched-in circuit-breaker type of automatic transfer switch to transfer the load from the normal to the emergency source in the event of failure of the former, and automatically transfer it back again on restoration of the normal source. In order to facilitate maintenance of the transfer switch there are by-pass and disconnect switches so arranged that the load can be by-passed around the transfer switch and the latter isolated, without interrupting the load. The remainder of the switchboard consists of the meter panel and branch feeder circuit breakers. The switchboards are all of dead-front steel construction with draw-out type of main service breakers. All circuit breakers are of the air-break type, main breakers having 50,000 amperes interrupting capacity, and the branch breakers in the switchboard 25,000 amperes interrupting capacity.

Pumps

The drainage pumps are located in pump rooms over sumps constructed below track level—see Figure 6. Each installation includes two pumps, one acting as a standby normally, and each consisting of a submerged centrifugal pump driven through a long shaft by a 208-volt three-phase squirrel-cage splash-proof motor automatically controlled by float switches. At locations where the subway crosses below the Chicago River the motors are located at the ground surface, and they drive the pumps below by 80-foot shafts. The motors—ranging from 5 to 50 horsepower—are equipped with line-voltage starters. In the State Street subway, there is a total of ten pump rooms.

Table I

	Kva
Lighting	270
Pumps	360
Fans	350
Escalators	370
Miscellaneous	280
Total	1,630

Ventilation

In general, it is proposed to depend upon the piston action of the moving trains for the normal ventilation of the tunnels, excepting in those portions of the tunnels which are at so great a depth as to make the cost of vent shafts to the surface prohibitive. These deeper sections of tunnel under the river will be ventilated by fans which can be operated continuously.

It is also proposed to depend upon the piston action of the moving trains for the normal ventilation of the stations, excepting the loop stations where the piston action will be somewhat ineffective.

Emergency fans will be installed midway between each pair of stations outside of the central district. In addition, small fans for the ventilation of toilet rooms and other service rooms will be provided in all of the stations. Vent shafts from train sections to the surface have been built on about 450-foot centers. The vent shafts near the stations have been located to serve a double purpose, namely, ventilation and blast relief for the station platforms. Without blast-relieving shafts at the tunnel entrances and exits of the stations, the high air velocities produced on the station platforms by the piston action of the trains would be intolerable. All of the openings in the tunnel walls to vent shafts will be equipped with power-operated louvers. These louvers will be interlocked with the emergency fans, so that when these fans are in operation, th louvers will be closed.

Two-speed reversible station fans wi provide normal ventilation at the loo stations.

The horsepower of the fan motor ranges from $7^1/_2$ to 60. All of the fan except one will be of the propeller type six to ten feet in diameter, and belt driven The motors will be two-speed three-phas squirrel-cage motors with splashproo frames. One fan will be of the centrifuga exhaust type driven by a 60-horsepowe motor and will be used to ventilate a deep tunnel section that does not have ventilat ing shafts. There will be a total of 26 fan rooms in the State Street subway includ ing 13 in the downtown stations. The fans will be controlled by the supervis ory control system of the railway.

Lighting

The normal illumination of the subway will be by fluorescent lamps in the stations and over station platforms, and by in candescent lamps in the train tubes Forty-eight-inch, 40-watt white fluores cent tubes will be used in the stations The tubes will be mounted in single-tube fixtures with single-tube power-factor correcting auxiliaries mounted in the base Each tube will be entirely enclosed by a frosted glass globe covering the lower half of the tube, and a reflector and the fix ture base covering the upper half. The lighting fixtures and lamps are planned to produce lighting intensities varying from six to more than eight foot-candles on mezzanine floors and station platforms.

Sixty-watt incandescent bare lamps will illuminate the tunnels between station platforms. They will be provided with opaque shields on one side to protect the eyes of the motormen. The lamps will be spaced at approximately 30-foot intervals on each of two lines of lamps per tunnel. Every fourth light in the tunnels will be on a 600-volt d-c circuit, with 56-watt railway-type lamps connected five in series in a group. Emergency 600-volt lights in the stations will be incandescent lamps.

The a-c lights will be remote-controlled from the cashiers' booths in the stations,

Figure 7. Streamlined and articulated subway-elevated cars of the type proposed for Chicago

single-phase latched-in contactors closing the lighting circuits. In general, there will be two degrees of a-c illumination in the tubes and the stations, with space sectionalization. The 600-volt emergency lighting circuits will always be energized from one of two sources, with an automatic transfer switch being provided at each station for both tubes and station. A special 600-volt lighting cable running the full length of the tubes will be one of these sources, and the nearest traction positive feeder will be the other source.

All lighting loads will have duplicate feeders, one of which will be exclusively for the lighting load, and the other may serve other loads or act as a standby emergency feeder for one lighting load, while serving as a normal feeder for a motor load. Manual transfer switches will be used for some lighting loads when near a switchboard, but the tunnel-lighting feeders will have automatic transfer switches. This duplication of lighting feeders will be in addition to the triple reliability of three-phase, four-wire lighting circuits with single-pole subbranch breakers serving the same area. Thus, the importance of reliable illumination in a metropolitan subway is recognized.

Provision will be made for more intense illumination at the portals during daylight hours, to permit adjustment of the motorman's eyes to the comparatively dark tunnels, by flood lights located near subway portals. Control of these additional lights will be from outdoor photoelectric cells.

Escalators

For the reason that the grade of the subway platforms is generally 40 feet or more below the sidewalks, the escalator installation in the State Street subway is exceptionally liberal. There will be two escalators connecting the mezzanine level with the platform level at almost all stations. Escalators are attractively finished and will operate at a speed of 90 lineal feet per minute. The downtown station escalators are four feet in width, while those in lighter outlying stations will be three feet. All the usual safety devices will be provided. The escalators will be reversible and will be controlled by push buttons located near the top and bottom of each escalator. The horsepower required to operate the various escalators will vary from 16 to 25 each when fully loaded. This equipment was purchased late in the year 1940 at the total cost of $1,174,000 for both subway routes.

Transportation Features

Train Equipment

It appears that the subway will be ready for operation prior to the time new rolling stock is available. The war is likely to cause an extensive delay in securing modern equipment for the proposed subway service. Therefore, operation will be initiated with trains of existing steel cars of the Chicago Rapid Transit Company.

There is a total of 455 of these units available—389 of which are equipped with two 170-horsepower motors each—and 66 trailers. The seating capacity is 52 passengers per car, and the total capacity 100. These rapid-transit cars were originally designed for use in a subway and are well adapted for the purpose. Trains will be composed chiefly of motor cars, insuring high free-running speeds and rapid acceleration.

The problem of new rolling stock was thoroughly studied by a committee of engineers, organized at the suggestion of the city, composed of representatives of the Illinois Commerce Commission, Chicago Department of Subways and Superhighways, the Chicago Rapid Transit Company, and the Chicago Surface Lines. The committee, after investigating the needs of the subway and the type of car best-suited to meet requirements of modern rapid-transit operation, drafted specifications for a modern lightweight high-speed rapid-transit unit.

A streamlined articulated unit, consisting of three compartment bodies on four trucks, and weighing approximately 82,000 pounds complete without load, was proposed. The general type of car is illustrated in Figure 7. The seating capacity is 106 passengers, and the total capacity 200. Propulsion would be with eight motors totaling 440 horsepower, and the free running speed 45 miles per hour on level tangent track. Such a car would start and stop at the rate of three miles per hour per second under average operating conditions.

In planning the new unit, the committee sought to utilize as many as possible of the favorable characteristics and equipment of the PCC street car. The latter vehicle, which obtains its alphabetical name from the Presidents' Conference Committee sponsoring its development, has been successfully used during the past six years for surface city transit. It embodies many "passenger-comfort" features, such as wide clear-view windows, plentiful illumination, adequate ventilation, uniform distribution of heat, smooth rapid starts and stops, noiseless operation, and the easy riding qualities resulting from rubber springs and resilient wheel construction. The operation of these new units over the welded running rails of the subway tracks promises an innovation in smooth-riding quiet subway-train operation.

The initiation of service on the State Street subway with the 455 steel cars now being specially equipped for subway use will provide three-minute service in each direction. When additional rolling stock is obtained, the number of trains will be increased with the demands of traffic up to a maximum of 40 trains per hour in each direction, the limit of the track capacity.

Operation

The maximum capacity of two-track rapid-transit subways is calculated by assuming the operation of 40 trains of five articulated units per track per hour. Units with maximum dimensions of 9 feet 6 inches in width and 90 feet in length, and weighing about 41 tons, are contemplated. With an average loading of 200 passengers per unit such an operation would develop a capacity of 40,000 passengers per hour per track. Thus the total capacity of the State Street subway would be 80,000 passengers, or 40,000 during the maximum half hour. This total capacity may be compared with the present total combined load for rapid transit, street cars, and motor busses of less than 90,000 in the maximum 30-minute period. Considering the fact that convenience requires the retention of some local bus and trolley-car routes at street grade, it is apparent that this terminal subway layout will provide adequately not only for present but for future traffic for years to come.

The 3,300-foot long platform planned for the State Street subway in the loop district will provide space for three double stops—each about 1,100 feet in length. Separate berths will be provided at each station stop for trains operating in different directions, thus providing the broadest possible distribution of incoming and outgoing passengers along the platform and through escalators, turnstiles, and stairways.

A New Multiple High-Speed Air Circuit Breaker for Mercury-Arc-Rectifier Anode Circuits and Its Relation to the Arc-Back Problem

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Synopsis: Mercury-arc power rectifiers have been used increasingly to supply power to d-c utilization systems in the United States since the early twenties. War requirements, because they have multiplied the demand for aluminum, magnesium, chlorine, zinc, and copper, all of which utilize d-c current in their reduction, have tremendously increased d-c power consumption. It is estimated that $12^1/_2$ per cent of the kilowatt-hours generated in 1941 were consumed in such electrochemical processes. Since the mercury-arc rectifier is now the almost universally accepted form of conversion apparatus for this purpose, the attention of many more engineers has recently been focused on it.

Many advantages of the modern rectifier have led to its wide acceptance, but it is not within the scope of this paper to deal with these. One persistent factor in connection with the application of rectifiers which has claimed much attention is the phenomenon of arc-back. Various methods of attack have been employed successfully. High-speed anode switching appears to be the most satisfactory way to handle this problem. Such a solution is widely used in the aluminum industry.[1]

Part I of this paper includes an analysis of the arc-back problem and various means of protection from its effects. Reasons why high-speed anode switching *is* an improved type of arc-back protection are set forth.

Part II of this paper describes this multipole high-speed air circuit breaker. The requirements which the breaker must meet are discussed, and the electrical and mechanical features described. Performance of the breaker was checked in field tests. Oscillographic data are presented and prove that the performance is acceptable.

Part I. Analysis of Arc-Back Problem

Definition of Arc-Back

ARC-back may be defined as a failure of the insulating properties in an arc rectifier during the inverse half wave which results in the rapid reversal of current through the rectifier element. When such an event occurs, there are usually manifestations of this failure, such as high short-circuit a-c current and reversal of d-c current normally flowing from the rectifier.

Various theories have been developed to explain the reasons for this occasional failure of rectifying property or arc-back.[2–6] One commonly accepted explanation is the "particle theory" developed by Doctor Kingdon.[3–5]

In this theory an assumption is made that a minute particle of insulating material, perhaps as small as 10^{-6} centimeter in diameter, becomes accidentally attached to the surface of the anode. Under certain conditions this particle may become charged by positive ions from the surrounding ionized plasma, and a positive voltage built up. Since the distance between the particle and anode is infinitesimal, a high enough gradient may be established between particle and anode to draw electrons from the anode surface (one to ten million volts per centimeter). When the anode emits electrons, it becomes a cathode, and the rectifier is in arc-back. The chances of such particles or patches becoming charged sufficiently to create arc-back is a statistical study.

Engineering Approach to the Arc-Back Problem

Ever since the first commercial mercury-arc power rectifiers were installed, engineers have struggled with the arc-back problem. Constant research and developmental work have been and will continue to be conducted to minimize the occurrence. It has even been the subject discussed at several informal conferences under the sponsorship of the AIEE electronics committee at winter conventions.

Rating and Arc-Back Frequency

Much progress has been made. During the past 15 years, reliable rectifier ratings have been increased from 750 kw per vacuum system at the outset[4] until today there are many units in very successful operation supplying as high as 3,750 kw per vacuum system.

For d-c output voltages below 300 the arc-back problem has not been difficult. Absence of arc-backs has permitted the engineer to base the ratings on thermal limits of the rectifier element and associated transformer equipment.

However, at higher voltages, increases in rating have required progress in the art of reducing arc-backs. Usually, the rating is based on the level of load which the rectifier will carry with acceptable arc-back frequency rather than on the thermal limits of the apparatus.

Anode Shielding

One of the most powerful means available to engineers of reducing arc-back tendency is to increase the deionizing shielding which is placed near the anodes. The shielding may be increased by placing more deionizing surface in the arc stream below the anode, by reducing the area of the holes in the grid through which the arc passes, by changing spacing and configuration, by increasing arc length, or by multiplying the number of grids. It must be realized though that most of these measures also result in increasing the arc losses and lowering the efficiency.

In the modern rectifier a satisfactory compromise has been reached. Sufficient shielding has been provided to insure acceptable performance as regards arc-back frequency. On the other hand, it has not been increased to the point where it has endangered the attractive higher efficiency that the rectifier usually possesses, as compared to other types of conversion apparatus. Sufficient shielding to reduce arc-back near the vanishing point would likely sacrifice that margin of efficiency.

Reactance

Another factor, which bears on arc-back tendency, is the value of reactance in the transformer supplying the rectifier, and in the a-c system supplying the a-c power. Engineers have learned that a given rectifier may arc back frequently when connected to a transformer and a-c system having certain reactance characteristics, whereas, the same rectifier may be completely free from arc-back for other circuits and reactance characteristics, even though the d-c output voltages and loads are identical. Advantage is usually taken of this factor in the struggle to minimize arc back.

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Protective Equipment

Designing engineers, after having arrived at an economic balance between efficiency and shielding, have produced rectifiers which are relatively low in arc-back frequency. However, they know that they are unable to predict zero arc-backs. Therefore, they must depend upon protective equipment associated with the rectifier apparatus to eliminate the effect of arc-back, both on the electric equipment and on the continuity of service required.

Paths and Magnitudes of the Arc-Back Currents

In order to select protective equipment it is necessary to study the direction of current flow, its path through the various electric circuits, and the probable magnitudes involved. Figure 1 shows typical power circuits for two six-anode rectifiers operating in parallel on the same d-c bus, supplying a counter electromotive force load. Arrows indicate the direction of direct current in the various parts of the circuit when an arc-back occurs in one of the rectifying elements. Figure 2 indicates the behavior of current flowing in the important parts of the circuit shown in Figure 1 as they would be seen in an oscillograph. Steady-state values are typical of what would occur in many conventional rectifier installations if protective equipment were not provided. Current values shown are typical, being values measured oscillographically during representative tests on rectifiers rated 5,000 amperes 600 volts direct-current.

Accurate mathematical prediction of the current magnitude which would flow in a given circuit would be extremely difficult. The most important reasons for this are that reactance values in any transformer involved in an arc-back do not remain constant because of d-c saturation of the iron; and voltage drops in the arcs are not well known under the high current conditions obtaining. Usually such attempts give results in magnitude somewhat higher than test measurements. Therefore, studies of this sort are usually based on actual oscillographic tests. Certain general rules however, may be used to estimate the behavior of arc-back currents.

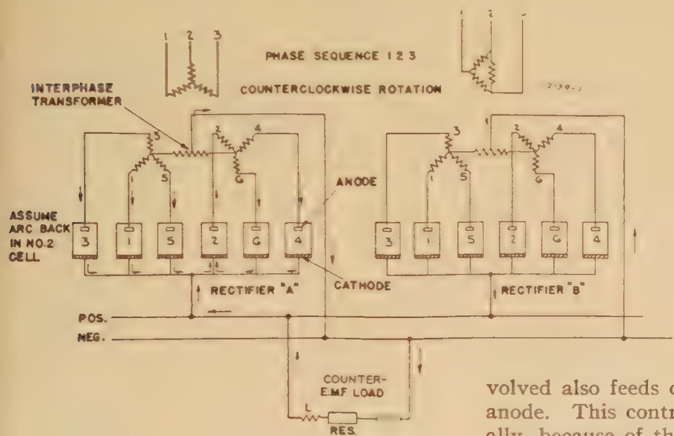


Figure 1. Direction of current in various parts of circuit when rectifier *A* arcs back

Rules for Behavior of Arc-Back Currents

1. The reverse current flowing in the anode circuit of the rectifier element in arc-back consists of several components:

Contribution From Same Wye. The two other phases of the same transformer wye feed currents into the affected anode in turn as they commutate. Their magnitudes are limited by the commutating impedance of the rectifier circuit. This component is alternating or pulsating in character being displaced from the zero axis by action of the rectifier.

Contribution From Other Wye. The other wye of the rectifier transformer involved also feeds current into the affected anode. This contribution increases gradually, because of the initial high impedance of the interphase transformer. As the interphase transformer saturates due to undirectional current, its impedance decreases, and this contribution becomes greater. Its magnitude is determined by commutating impedance. This component is displaced from the zero axis by the action of the rectifying elements.

Contribution From Parallel Conversion Apparatus. Another component is fed from any parallel conversion apparatus such as other rectifiers connected in parallel to the same bus. This component is not pulsating being direct current in character. Its rate of rise is determined by the reactance due to air leakage fluxes throughout the circuit, both in the leakage spaces of the transformer windings conducting the reverse current and in the connections between the rectifiers. Final or steady-state magnitude of this component is limited only by the metal resistances of the station conductors in series, the resistance of one-half transformer secondary winding, the resistance of one-half the interphase winding, and the lumped or equivalent resistance of all contributing conversion apparatus in parallel.

Contribution From the D-C Load. If the nature of the d-c load is counter electromotive force, it too will feed current through the anode in arc-back. This component is d-c in character. Its rate of rise is determined by the air flux reactance of the circuits involved and its steady-state value by the total series metal resistance of the circuit.

2. All of these components add arithmetically to give the total reverse current at any instant carried by the anode in arc-back.

3. The effective voltage driving the short-circuit currents is reduced in proportion to the increase in arc drops caused by the higher currents passing through the arcs. Values for this increase in arc drop have been difficult to obtain, partly because of the transient and unpredictable nature of arc-backs and partly because of the instability of high-current arcs. Estimates of 50 to 100 volts are often used.

4. The effective driving voltage is reduced by a-c system regulation.

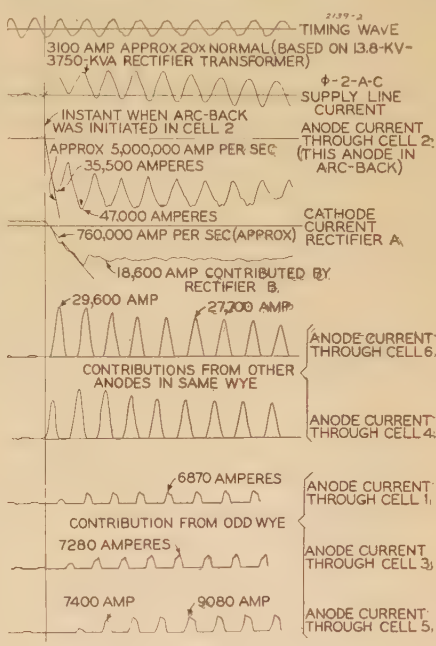


Figure 2. Behavior of current in various parts of circuit during arc-back

5. In practice it is found that the major contributions to the total reverse current are from the same wye and from parallel conversion equipment. The latter contribution is increased as the number and rating of paralleled conversion apparatus on the same d-c bus is raised. Such contribution theoretically reaches alarming proportions for many large electrochemical installations. It has been estimated that theoretical steady-state values of reverse current in an anode circuit might reach 600,000 amperes for certain large installations. (Anode reverse-current rates of rise for large installations have been measured and range as high as 10,000,000 amperes per second.)

6. Other anodes in the same rectifier unit or in other rectifiers on the same d-c bus are caused to arc back as a result of their high contributions to the first one in arc-back. It is doubtful if such theoretically possible high steady-state values as 600,000 amperes would be reached, and certainly they would not be maintained. Sympathetic arc-backs would always spread among the

associated rectifiers. End result would be that all rectifiers would become involved, and all contributions from parallel apparatus vanish.

7. Contributions from d-c loads usually are transitory. Rotating-type d-c loads come to a standstill quickly. Electrolytic loads are usually inductive, so that the current reversal is relatively slow, and rate of rise limited. Electrolytic-cell polarization voltages are usually only a fraction of the d-c applied voltage, and resistance drops are a large factor. Under short circuit such cells lose their polarity rapidly.

8. Current increases in the primary windings of the rectifier transformer corresponding (by transformer action) to the secondary currents. The values would be similar to those reached when all the secondary windings of such a transformer are short-circuited, except for the fact of the rectifying action and the higher-exciting currents due

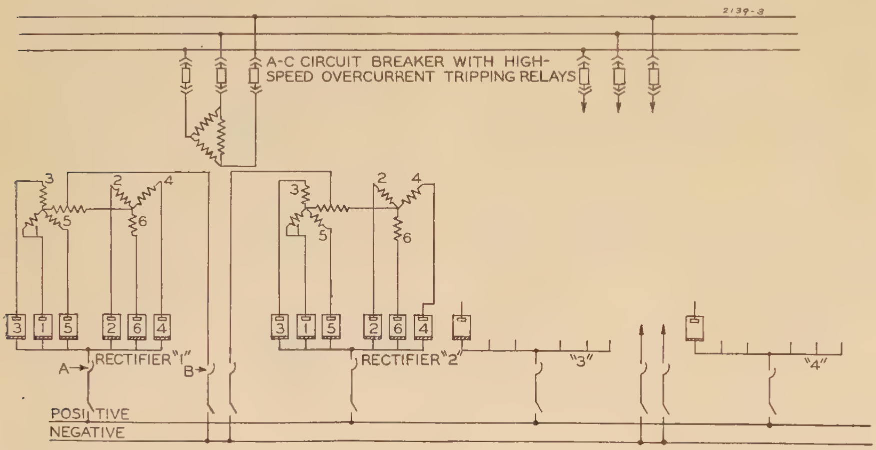


Figure 3. Cathode-switching arrangement of protective equipment

A—High-speed d-c circuit breaker with built-in reverse-current tripping action

B—Medium-speed d-c circuit breaker with time-delay overcurrent trip

to d-c saturation in the iron. These factors increase the primary currents over the values that would obtain if all the transformer secondary windings were merely short-circuited.

Effect of Arc-Back Without Protection

The preceding analysis indicates that the high currents impose stresses on nearly all parts of the circuit. Various injurious results which could be expected if protective equipment were not provided may be listed as follows:

1. The a-c supply system may be overloaded. Increase in current supplied during arc-back often reaches 20 times the full-load current rating of the transformer.

2. The rectifier transformer would be damaged. Rectifier transformers are usually specially braced to withstand mechanical shocks due to arc-back. The duty caused by the high current in the windings connected to the anodes in arc-back would be particularly severe.

3. The rectifier element itself would be affected in several ways:

A. The higher currents would heat parts not previously degassed for such high currents and drive off gases which would impair the vacuum.

B. The high-current arcs would burn the metal walls of the vacuum chamber.

C. While the graphite anodes will withstand severe heating, continued presence of a cathode spot at the anode would remove material and roughen the surface.

4. Continuity of service would be interrupted, because other conversion apparatus on the same bus would be overloaded to the failure point.

Transients Which Signal Arc-Back

It is obvious that the sudden reverse and overcurrent conditions which appear in the circuits during arc-back offer ready means for signaling the event and actuating the protective equipment. A study of Figures 1 and 2 in the light of these requirements is worth-while. Current suddenly increases in the a-c supply lines and in all the anodes operating in normal forward-current fashion. However, these are symptoms of overload as well as arc-back. It is difficult by this means to distinguish arc-back from plain overload. If there is parallel conversion apparatus, a reliable indication of arc-back is the presence of reverse current in the cathode and negative connections. However, where a single rectifier feeds a purely resistance-type d-c load, no reversal occurs in these connections. The invariably present unmistakable evidence of arc-back is the presence of reverse current in the connection between the transformer secondary winding and the anode of the rectifier.

Four General Methods of Arc-Back Protection

Broadly there are four methods of protecting from arc-back. Three of these have been used in conventional large power installations.

Reactance

First is the use of large reactance values to limit the arc-back currents. Often if the resulting flow of current is limited to relatively low values by reactance, the condition causing the arc-back at the anode disappears, and normal rectifier action is automatically re-established. This can be true only where no other counter-electromotive-force apparatus is connected to the same d-c system. This method is sometimes used for such rectifier installations as those furnishing high-voltage d-c power to dust precipitators, oil-cracking processes, and laboratory apparatus.

Arc Suppression

Second is the so-called "arc-suppression" method. Arc suppression—in the case of multianode rectifiers—consists of grid action to prevent anodes from firing; in the case of ignitron rectifiers—it consists of ignitron action and grid action to prevent anodes from firing. Arc suppression does not have the ability to interrupt current passing from anode to cathode once it has been established.

"Arc suppression" accomplishes its purpose by preventing anodes—which would normally contribute current to the one in arc-back—from firing in turn as they become successively positive in voltage. In the case of a single rectifier, the anode reverse current consists entirely of contributions from other anodes, and it is possible quickly to starve the affected anode and eliminate the arc-back. It should be added that, if other conversion apparatus is connected to the same d-c bus, the current which they contribute must also be interrupted. One method is to interrupt the reverse current in the cathode line by means of a high-speed d-c circuit breaker. Another method, assuming all other conversion apparatus consists of rectifiers, is to apply "arc suppression" simultaneously to all other rectifier elements connected to the same bus during arc back.

"Arc suppression," by means of grids, is accomplished by suddenly energizing the grid with negative potential. With a negatively charged grid between the anode and cathode spot, electrons are repelled, and the anode is often effectively blanketed so that it cannot fire. In the ignitron, the anode is prevented from firing by suddenly cutting off the electric excitation of the igniter point which creates a new cathode spot each cycle. Without the existence or creation of the cath-

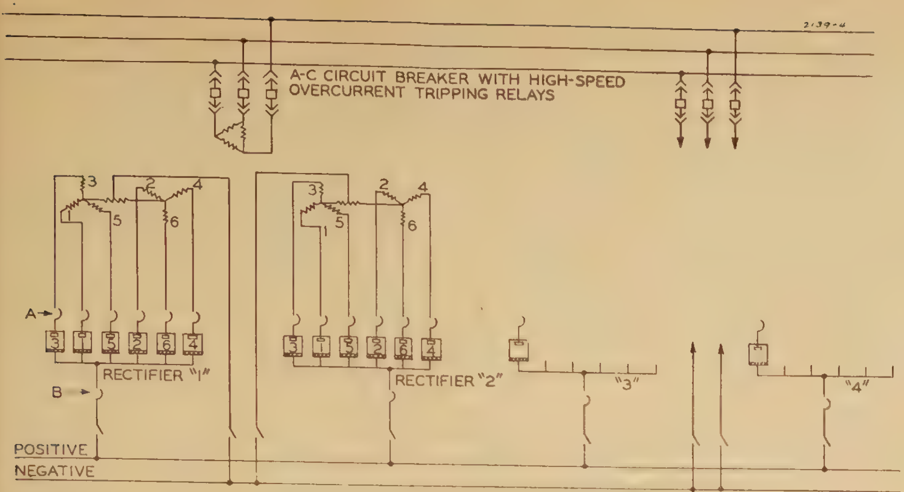


Figure 4. Anode-switching arrangement of protective equipment

A—High-speed multipole-anode circuit breakers with built-in reverse-current tripping action for each pole

B—Medium-speed d-c circuit breakers with time-delay overcurrent trips

ode spot in the mercury cathode pool, the anode should not fire.

Experience with "arc suppression," however, indicates that it is not always effective. Therefore, other backup protection in the form of protective switchgear is always provided to interrupt the overcurrents when "arc suppression" fails.

When it is successful, "arc suppression" is so rapid in action (one-half to two cycles) that it is not necessary for the a-c circuit breaker to open. The yardstick often used to measure the effectiveness of "arc suppression" is to record and compare the number of arc-backs accompanied by a-c breaker operations with those unaccompanied by a-c breaker operations. It is obvious that the relay or device which detects the presence of arc-back and actuates the "arc suppression" must be designed to operate in a small fraction of a cycle.

Experience indicates that "arc suppression" appears to be more successful in installations where reactance values are relatively high.

Cathode Switching

Third is the method most commonly employed until recently—that of disconnecting the rectifier and transformer from the system by means of circuit breakers. A typical arrangement is shown in Figure 3. In its best form it consists of an a-c circuit breaker with special high-speed induction-type a-c overcurrent relay protection, for each rectifier transformer, and a special high-speed reverse-current d-c air circuit breaker in the d-c side for each rectifier.

To illustrate the operation of the cathode-switching arrangement, assume an arc-back in cell 2 of rectifier 1—Figure 3. The following action takes place:

1. The d-c current reverses in the cathode line of rectifier 1. This action trips the reverse-current high-speed d-c breaker, which starts reducing the current in one-half to three-fourths cycle and interrupts the d-c circuit. Reverse current in the cathode line had been contributed by rectifiers 2, 3, and 4. This duty imposed overloads on these units and had started to operate the overcurrent tripping relays associated with the a-c circuit breaker supplying rectifiers 3 and 4 and all the negative d-c-line breakers. However, the high-speed action of the above reverse-current breaker usually should relieve the short circuit in time to prevent outages on 3 and 4, unless they too arc back sympathetically.

2. Even though the reverse-current contribution to cell 2 from the other parallel rectifiers is removed quickly by the high-speed cathode breaker, contributions from the other anodes in the same rectifier continue. Overcurrent relays associated with the a-c circuit breaker protecting the main transformer for rectifiers 1 and 2 therefore complete their operation, disconnecting the transformer and both rectifiers 1 and 2 from the a-c system.

3. Conventional a-c power-circuit breakers require a minimum of seven to ten cycles including relay time to interrupt an a-c short circuit. This means that the heavy duty imposed by the high arc-back current in both the rectifier element and the transformer winding continues throughout this time.

4. Two rectifiers connected to one transformer have been disconnected from service because of an arc-back in only one unit, as it was necessary to open the a-c power circuit breaker. Only rectifiers 3 and 4 remain in service.

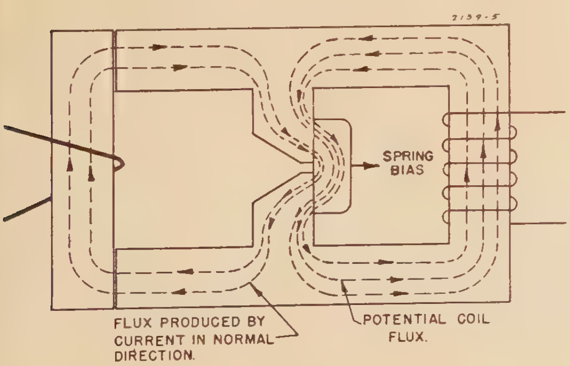
Anode Switching

Fourth is the latest method of dealing with the problem. A high-speed air circuit breaker in the anode line interrupts the fault current resulting from the arc-back.

Such an arrangement is shown in Figure 4. In its best form it consists of a power circuit breaker with standard inverse time a-c overcurrent relay protection for each transformer primary winding, a special high-speed multipole circuit breaker in the anode circuits with reverse-current tripping action, and a standard medium-speed d-c air circuit breaker in the cathode line for each rectifier. To illustrate the operation of anode switching and compare it with operation of cathode switching, assume an arc-back in cell 2 of rectifier 1, Figure 4. The following action takes place:

1. The d-c current reverses in anode line 2. This action trips pole 2 of the high-speed multipole-anode air circuit breaker, which starts to reduce the overcurrent in one-half cycle or less and interrupts the arc-back current in less than one cycle. Current contribution from rectifiers 2, 3, and 4, as well as the other anodes in the same rectifier unit, are all cut off. The common circuit path where they all add up and flow has been opened.

2. Since the short-circuit condition in the



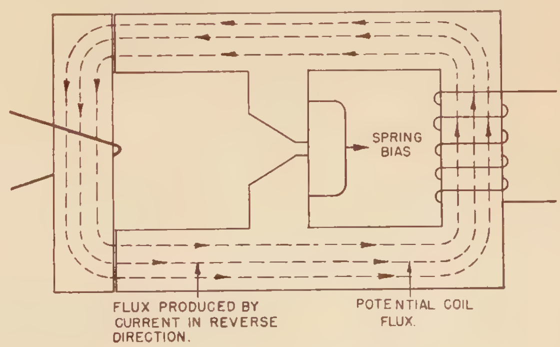


Figure 5. Magnetic circuit of high-speed trip mechanism, forward and reverse directions

transformer supplying rectifier 1 has disappeared in one cycle, its a-c overcurrent relay does not trip the main breaker. Therefore, the transformer remains energized, and rectifier 2 may remain in service.

3. High-speed action of pole 2 in interrupting the reverse current removed the overcurrent condition imposed on rectifiers 3 and 4 soon enough to prevent their outage.

4. Because the duration of short circuit does not last more than one cycle, only a small fraction of the same duty is imposed on both rectifier element and transformer winding of rectifier 1 as compared to the longer duty imposed with cathode switching when the overcurrent lasts 7–10 cycles.

5. The medium-speed cathode breaker can be interlocked with all poles of the anode breaker, so that the cathode breaker serving rectifier 1 would open, but without severe duty. Interlocking would be employed where it is inadvisable to allow a rectifier to remain in service without all anodes in operation.

Cathode switching for arc-back protection was adopted early in the history of rectifier application. It had wide acceptance, but "arc suppression" was later developed as recognition of some of the cathode-switching limitations, grew. Then when experience proved that "arc suppression" was not dependable under all conditions, and because wide use of rectifiers justified the effort, the high-speed anode switching was introduced. It now appears that the use of adequate high-speed anode breakers fulfills the requirements for arc-back protective equipment.

Part II. The High-Speed Air Circuit Breaker Developed

Basic Requirements

Before embarking upon the design of a new multipole high-speed breaker to satisfy the requirements of anode protection for mercury-arc rectifiers, a study was made to determine the prime requisites of this new breaker. These are as follows:

(*a*). The breaker should have a high opening speed. To clarify this it was determined that the breaker should start to reduce line current a half cycle or less (60-cycle basis) with maximum rates of current rise in the range of 2,000,000 to 6,000,000 amperes per second.

(*b*). Each pole of the breaker should be independently tripfree and be provided with a reverse-current tripping device, direct acting so that an arc-back on any anode would be removed independently of the other anodes.

1. A breaker should consist of six of such poles, closed simultaneously or individually.

(*c*). The breaker should remain closed on excessively high currents in the forward direction to insure that a rectifier not involved in an arc-back would remain in service.

(*d*). The proposed application required a breaker which could remain closed carrying load for long periods of time. It was therefore evident that existing switchgear standards should not be sacrificed in the interest of breaker opening speed.

There were other miscellaneous minor requirements attendant to the design of this new breaker, such as electrical closing, mounting, and so forth, which, while of interest to the designer, are not so pertinent to the discussion of this new high-speed breaker as the four preceding main requirements.

Design Problems

An analysis of the above requirements indicated that four major design problems were involved. These were as follows:

1. A high-speed current directional tripping mechanism.
2. A suitable breaker structure.
3. A current-interrupting device, compact in size, yet capable of coping with voltage in the order of 1,700 between phases.
4. A simple drive for operating a number of poles mechanically in parallel at a reasonably constant speed, regardless of the number of poles to be closed.

Trip Mechanism

The magnetic circuit of the tripping device selected is shown in Figure 5. The energy necessary for tripping the breaker on arc-back is stored in a compression spring restrained normally by the small armature. Flux provided by a potential coil normally holds the armature in the restrained position. A reversal of current caused by an arc-back quickly shifts the flux path of that portion normally holding the armature in the sealed position; therefore, upon current reversal the armature is released and allows the stored energy of the spring to dissipate itself in tripping the breaker latch. Figure 5 shows the flux path under normal operation and on arc-back. It will be noted that flux shifting is readily accomplished with this type of magnetic circuit.

The moving parts of the device were necessarily extremely light in order to reduce the effect of inertia and obtain a satisfactory operating time. The weight of these parts, excluding the spring, amounts to 2.4 ounces. The portion of the magnetic circuit in which the flux shifts, due to current reversal, was made of laminated silicon steel in order to facilitate the flux change. The magnetic circuit associated with the potential coil proper was made of solid iron and the iron surrounded with copper, so as to obtain a structure resistant to any sudden changes in applied voltage.

Since this device would be subjected to widely varying rates of current rise (in the order of 1,000,000 to 10,000,000 amperes per second), it was necessary to determine the effect of an extremely high rate of rise on its performance. It was conceivable that with an extremely high rate of rise of current in the tripping direction, the flux in armature would be reduced to zero and then built up in the opposite direction before the armature had moved sufficiently to be free of the influence of this flux. Such a condition would make the device inoperative and would prevent its functioning to trip the breaker. The level at which this would occur could be controlled by varying the inertia of the moving parts and by reducing the air gap of the poles bridged by the armature, the final air gap arrived at by test being in the order of $^{3}/_{32}$ inch.

A rather novel and interesting method was adopted to determine the effect of widely varying rates of current rise on the performance of the device. This consisted of testing the device in a low-voltage high-current a-c circuit. For a half cycle of current in the normal direction the tripping armature would not release, but on the following half cycle of current in the reverse direction this armature would release to trip the breaker. By simply changing the current magnitude, rates of current rise in the order of 23,000,000 amperes per second were readily obtainable, and the release mechanism was tested and operated satisfactorily at this rate of rise in a time of 0.002 second from current zero to point of release. A typical oscillogram of this performance is

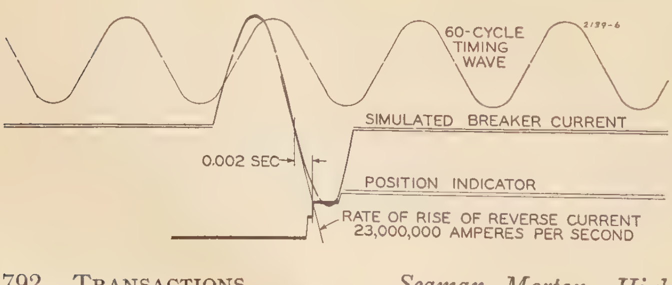


Figure 6. Typical oscillogram of low-voltage high-current a-c tests on high-speed trip mechanism

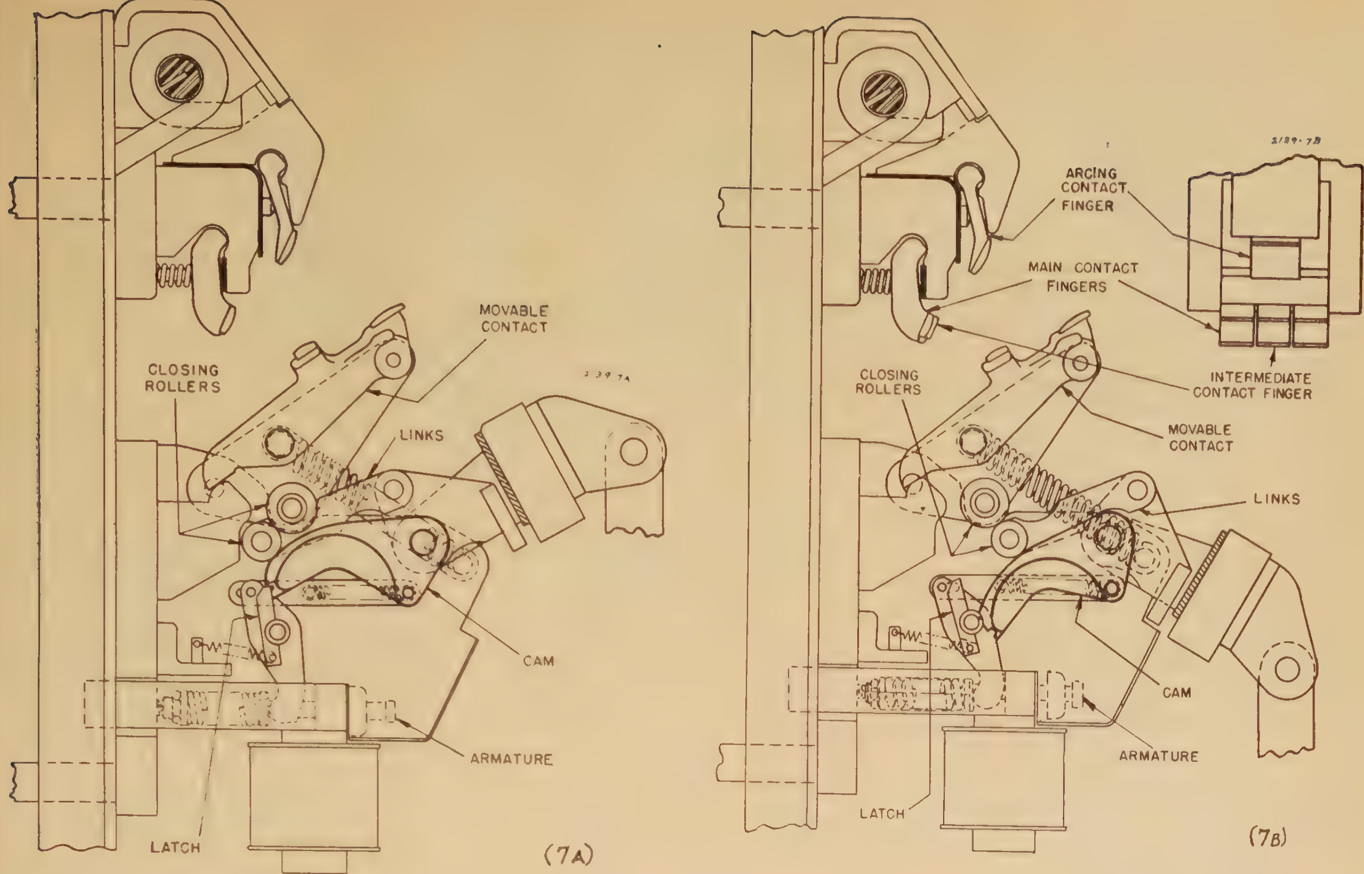


Figure 7A and B. Side views of breaker mechanism and contacts in open and collapsed positions

shown in Figure 6. Inasmuch as satisfactory performance was obtained at this rate of current rise, which is considerably above the rate of current rise obtainable in practice in d-c circuits, the design of the tripping mechanism was considered acceptable.

Breaker Structure

The tripping device just described was mounted on a breaker of conventional structure during the tests in the high-current circuit, both as a convenient method for testing the device itself, and also to observe the effects of high-speed operation on the mechanisms usually associated with more or less conventional circuit breakers.

As a result of these observations a novel breaker structure was evolved. Figure 7A illustrates the mechanism and contact arrangement designed for the application, with the mechanism reset ready to close. Positioned before the armature is a rotative latch normally supporting a cam-shaped member. The cam is so shaped that drawing a roller over its surface, by means of lever-actuated links, causes the movable contact to move to the fully closed position. Opening is equally simple to accomplish. Releasing the armature (Figure 7A) causes the latch to rotate from under the cam. The cam, closing rollers, and movable contact then collapse simultaneously. The opening speed is thereby dependent only on the applied opening force. Toggles with their hard-to-accelerate characteristics, under normal usage, are entirely eliminated. This mechanism represents a considerable departure from the usual toggle-activated circuit-breaker mechanism which does not lend itself so well to high-speed operation and is responsible to a considerable degree for the high-speed opening performance obtained with this new circuit breaker.

The contact arrangement designed for this high-speed breaker represents a considerable advance in the art of circuit-breaker design. Bearing in mind that it was considered desirable to design a breaker which would successfully withstand any tendency to blow open on currents of high magnitude in the forward or normal direction, it was decided to advantageously use the magnetic forces rather than to resort to brute force which would lead to a massive hard-to-accelerate structure. To this end the contacts were arranged so that the magnetic forces would add to the contact pressure rather than subtract from it.

Figure 8 shows that all points of contact are beneficially affected by the magnetic forces. The path of the current at the arcing contacts, main contact fingers, and at the lower end of the movable bridging contact is reversed sufficiently to "Magne-seal" then closed against currents of high magnitude and to gain equally from the accelerating effect of the magnetic forces when the restraint of the operating mechanism is removed.

Several other advantages accrued from this contact construction other than those listed above. A one-piece relatively light movable contact member was obtained which permitted the accomplishing of the high-speed operation objective. The pivoting of the lower end of this contact member on a fixed point on the lower stud permitted the eliminating of a heavy flexible connection which would otherwise have been mandatory. The difficulties experienced in designing adequate life into heavy braids capable of carrying currents in the order of 1,600 amperes with reasonable temperature limits and in overcoming their inherent resistance to angular movement are well known. The mounting of the arcing and main contacts on the upper stud resulted in light low-inertia parts which solved the problem of obtaining proper contact sequence between the primary and secondary circuits. Proper contact sequence is especially difficult to obtain with high-speed operation if the parts involved in the sequence are of relatively high inertia.

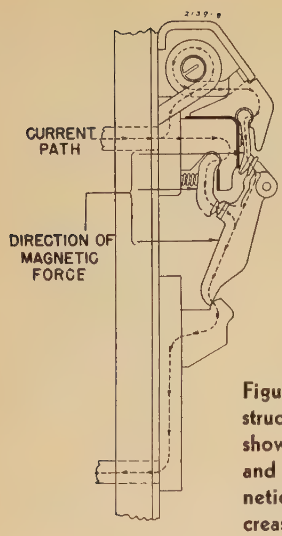


Figure 8. Contact structure closed showing current path and effect of magnetic forces to increase contact pressure

Three fingers were used for the stationary main contact. The middle finger was arranged to lead the other two slightly, so as to minimize burning on the other two fingers and to facilitate current transfer from the main to the arcing contact circuit. As a general result, a contact structure which is suitable for high-speed operation and which would satisfactorily meet existing switchgear standards as to temperature rise and reliability was obtained. Since electrochemical processes generally signify continuous full-current operation, this new contact structure for high-speed breakers provides a practical answer to continuity of service, by virtue of having both main and arcing contacts, generally considered necessary for continuous low-temperature-rise operation.

Current-Interrupting Device

The current-interrupting device designed for this breaker is extremely simple in construction and in operation. Figure 9 is a side view showing the component parts. The interrupting unit consists of a simple open-type arc chute and two blowout coils. One coil is in series with the arcing contacts on the upper stud of the breaker. Associated with this coil is an iron core and two pole pieces of conventional construction which act upon the arc terminus on the arc runner and the arc proper. The other coil is in series with arc runner located over the movable contact. This coil has only an iron core (no pole piece) which acts only on the arc terminus when it strikes the arc runner.

Successful operation is based entirely on rapidly increasing the length of the arc, therefore, the resistance, to the point where extinguishment takes place. Evidence of the effectiveness of this simple chute is shown in Figure 10. From current peak to zero is a smooth unbroken line

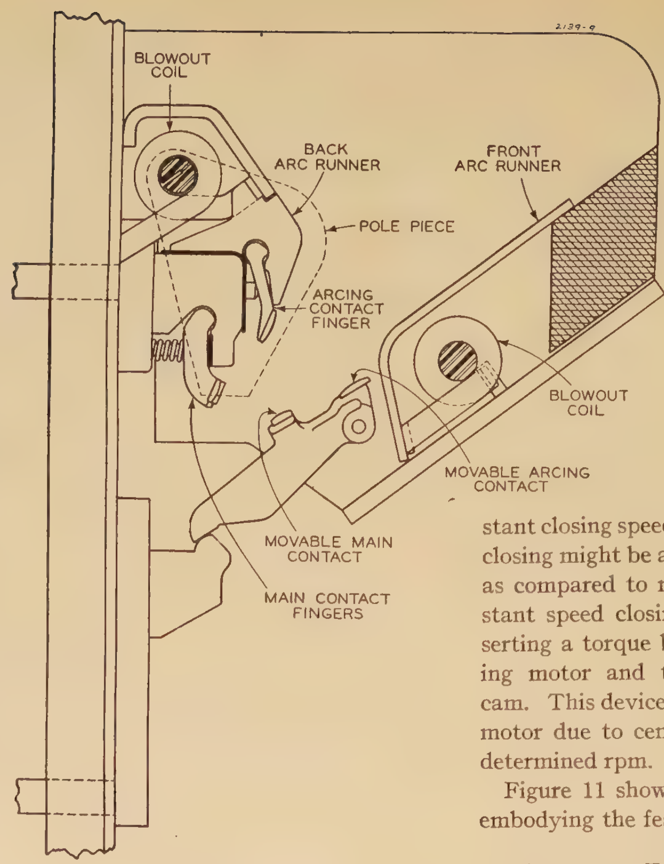


Figure 9. Side view of contacts (open) and arc chute, showing blowout coils

descending at a rapid rate, an excellent illustration of efficient current interruption. The advantage of this type of chute is that, having been designed for a specific voltage level, it has no current limitation. Any increase in current merely increases the magnetic forces which in turn speed interruption.

Closing Drive

Since rectifier anodes are operated in groups of six or multiples of six, it was decided that six individual breaker poles, each with its independent current directional trip, would be mounted on a common panel. A motor-driven closing mechanism was selected as the most suitable means for obtaining reliable closing performance. This mechanism consisted of a series motor suitable for either a-c or d-c operation, a torque brake, and a gear-driven closing cam. This complete unit was connected to a closing crossbar affixed to the closing lever of each individual pole. Operating the closing crossbar through 360 degrees of cam travel would cause any number of poles in the open position to be closed. This was particularly desirable since on arc-back with high-speed breaker operation, the arc-back is removed before all anodes feeding into the rectifier are caused to arc back. Since under this condition the closing load on the motor drive can vary widely, it was desirable to obtain a relatively constant closing speed. Otherwise single-pole closing might be accomplished too rapidly as compared to multipole closing. Constant speed closing was obtained by inserting a torque brake between the driving motor and the gear-driven closing cam. This device functioned to brake the motor due to centrifugal force at a predetermined rpm.

Figure 11 shows the completed design embodying the features just described.

Performance Tests on the High-Speed Anode Breaker

Factory Tests

During the progress of development of this new high-speed anode air circuit breaker, several representative short-circuit tests were made in the factory testing department. After the final design had been reached a complete six-pole model was built and tested in a rectifier circuit in the factory. Figure 12 indicates the test arrangements made and power circuits used.

Results of these tests were gratifying, but it was realized that the test conditions imposed might not be so severe as would be encountered in the field, especially for large electrochemical pot lines. The rectifier transformer used during these tests and the a-c system capacity were such that current contributions from other anodes in the same rectifier which fed into the anode in arc-back were as high as would be usually encountered in the field. However, it was impossible to duplicate d-c contributions from parallel rectifiers such as would be encountered in large electrochemical installations. Instead, an 1,800-kw 600-volt motor-generator set was connected to the same bus, but this unit lacked capacity for such large d-c contributions as were anticipated in the field. It also required its own high-speed breaker protection to prevent commutator damage, so that the duration of

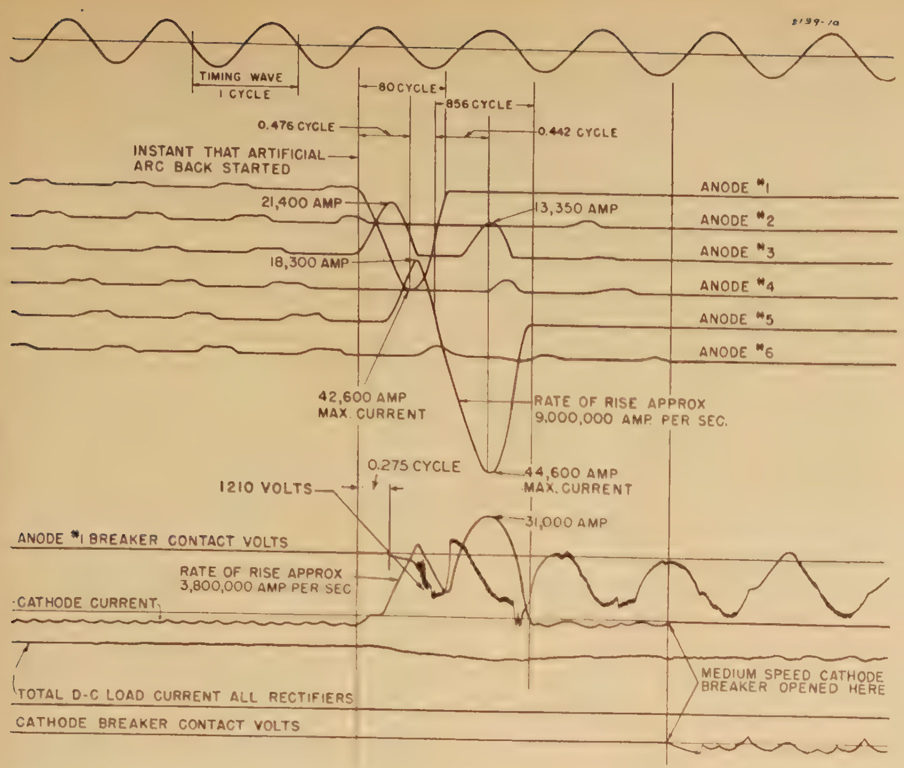


Figure 10. Typical oscillogram taken during field tests

its current contribution to the arc-back was limited.

Field Tests

Therefore, it was felt advisable to confirm final factory tests by making operating-speed and interrupting-capacity tests in a large rectifier installation. Arrangements were made for such tests. This rectifier installation consisted of eight 650-volt 2,600-kw rectifiers, supplied from four rectifier transformers, all connected to the same d-c bus. Short-circuit capacity at the 13.8-kv a-c supply bus was approximately 400,000 kva. D-c load during the test period averaged 20,800 kw on the eight rectifiers.

Figure 13 shows the one-line power-circuit diagram for the eight rectifiers and test arrangements for the rectifier and six-pole high-speed anode circuit breaker. Two six-element oscillographs were used to record current and voltage behavior in the various circuits simultaneously.

Since arc-backs cannot be produced at a predictable instant, it is necessary to simulate an arc-back by short-circuiting one rectifier element with a switch at the desired instant. Such a method imposes short-circuit currents which exceed the values reached under natural arc-back conditions, because they eliminate the effect of arc drop in the rectifier element in arc-back. Thus such test conditions are in error in the conservative direction. A large electrically operated power circuit breaker was used as the short-circuiting switch during the tests. No overcurrent tripping action was provided with this switch and so it remained closed until it was opened manually after each short-circuit test was conducted.

This particular installation had been made with the cathode switching-type arc-back protective equipment on all rectifiers. During the tests the regular high-speed cathode breaker remained in the open position at all times. A suitable 4,000-ampere medium-speed air circuit breaker with overcurrent tripping device was substituted instead.

Oscillographic Results

Figure 10 is a reproduction of an actual oscillogram obtained during the field tests. Several such oscillograms were taken, and this particular one is typical of the maximum fault conditions recorded.

Trace for anode 1 shows the current through the short-circuiting switch. It is apparent that the instant of switch closure occurred during the time of normal forward-current conduction for anode 1. Performance of pole 1 of the high-speed anode circuit breaker is clearly indicated on the oscillogram. Maximum current contributions come from anodes 1 and 5 and from the other paralleled rectifiers through the cathode line.

An interesting piece of incidental data obtained in this test is the record of the natural arc-back in anode 5. After its high forward-current contribution, it immediately went into arc-back as proved by the reversal of current. Performance

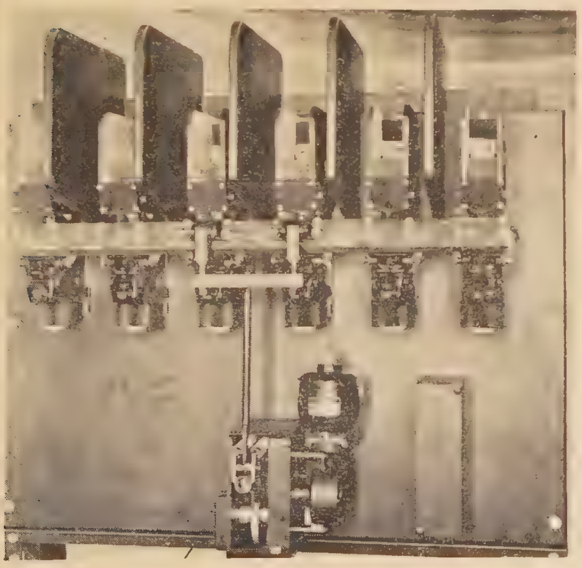


Figure 11 (left). The new high-speed breaker

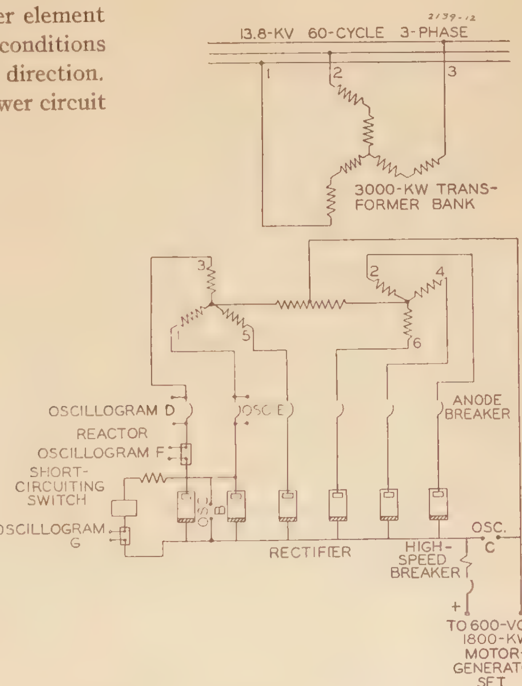


Figure 12. Factory-test circuit

of pole 5 is clearly indicated by the current trace for that anode.

Time required for contacts of pole 1 to part after current reversal was 0.275 cycle as measured by the trace of "anode 1 breaker contact volts." Current reduction as a result of arc quenching in the arc chute always started appreciably earlier than one-half cycle, according to the oscillograms taken during each test. The values of 0.476 cycle and 0.442 cycle illustrated in Figure 10 were typical.

Study of the cathode-current behavior shows that the individual poles each contributed interrupting effort, not only to that of the currents from the other anodes, but also to those from other rectifiers as well. In fact, pole 5 successfully interrupted all contributions. This is proved, because the traces for anodes 2, 3, 4, and 6 show normal forward-current rectifier operation lasting approximately $1\frac{1}{4}$ cycles after the last reverse current has been interrupted by pole 5.

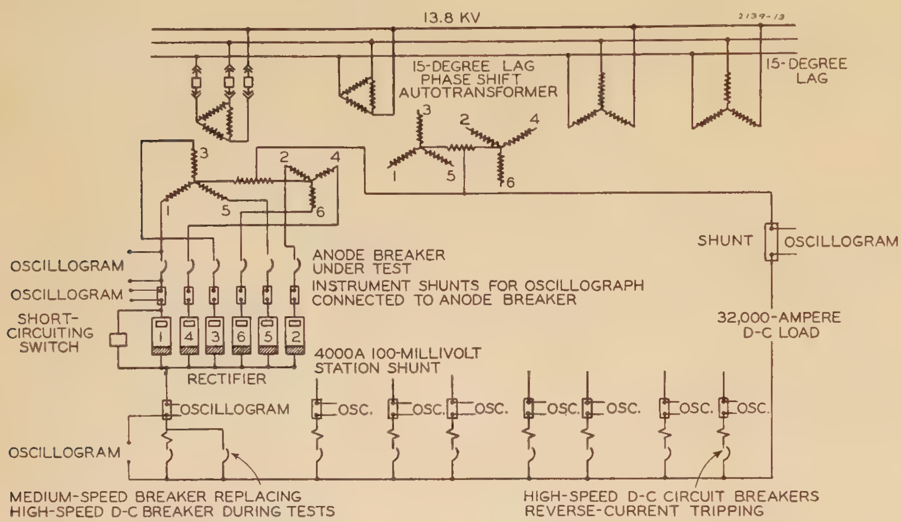


Figure 13. Field-test circuit

Traces of anode current for anodes 2, 3, 4, and 6 cathode current, and medium-speed cathode-breaker volts indicate that regular rectifier operation may have continued without distress on the remaining four anodes, had service not been interrupted by the operation of the cathode breaker. This circuit breaker interrupted less than normal full-load current when it opened $2\frac{1}{2}$ cycles after the event.

None of the several short-circuit tests conducted caused arc-back in any of the other rectifiers connected to the same d-c bus. Evidence of continuity of service is indicated by the trace of the "Total D-C Load Current All Rectifiers" in Figure 10. During these tests there were no a-c power circuit-breaker operations for the main rectifier transformer included in the test circuit. The companion rectifier connected to the same rectifier transformer always remained in service.

Surge-voltage recording instruments were connected across the interphase transformer and anode to neutral low-voltage main-transformer windings to detect any evidence of surge voltages which might result from the high-speed breaker action. No evidence of surging was recorded.

Oscillographic tests were conducted to determine the time required to interrupt normal load on the rectifier when tripping all six poles by means of removing polarizing coil voltage. Approximately $1\frac{1}{3}$ cycles were required after the switch in the polarizing circuit was opened.

Condition of Anode Circuit Breaker After Tests

A total of six tests were made in the field using the circuit shown in Figure 13. In addition to obtaining satisfactory high-speed operation, the results were particularly gratifying, in view of the condition of the breaker after the completion of the tests. The contacts were in excellent condition and justified the adoption of this arrangement for commercial use. No parts would have required replacement to continue in service. The condition of the breaker after tests and the high-speed performance obtained during tests clearly indicated that a new and useful tool had been provided to cope with mercury-arc rectifier arc-backs.

Conclusion

The advantages obtained by using such high-speed anode breakers include:

1. Shock to the transformer is reduced.

2. Shock to the rectifier is reduced.

3. Maintenance on a-c power circuit breaker is reduced, because it no longer is required to interrupt the high currents due to arc-back.

4. The vacuum in the rectifier is not so adversely affected when the arc-back is limited to one cycle or less, as compared with the longer time for a-c power-breaker operation. This means any rectifier which arcs back can be returned to service at once, whereas time out for pumping is often necessary with cathode-switching protection.

5. When two rectifiers are connected to the same transformer, only the one which arcs back is subjected to outage. The other remains in service. This has a most important bearing on improved continuity of service in many cases.

6. Likelihood of simultaneous arc-backs or spread of arc-backs to other parallel rectifiers is minimized. The reason is that the short circuit on the d-c bus is removed in one cycle or less. Again continuity of service is improved.

7. The anode breakers are useful disconnecting devices when two rectifiers are fed from a single transformer. They lend themselves to the isolating of one rectifier for maintenance or repair work without interrupting operation on the other rectifier.

8. High overcurrent duty is confined to one device for the anode-switching arrangement, whereas with cathode switching both cathode switch and a-c power circuit breaker open under high duty requirements. Maintenance is thus reduced.

9. Since each pole of the six-pole anode breaker opens independently of the other poles, and each pole is equipped with an operation counter, a record is provided of the anode arc-backs. This often is advantageous in locating and correcting arc-back trouble.

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Field Harmonics in Induction Motors

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I. Introduction

ABOUT 40 papers have been written on the asynchronous torques, synchronous cusps at running, synchronous cusps at standstill (dead points), and noise of the induction motor. The subject has been also treated in some textbooks.* My aim is to give a coherent representation of the harmonic problem in induction motors. It is based on the work of my predecessors, on my own work, and on my experience.

It is intended to give not only a physical explanation of the phenomena but also to determine them quantitatively, that is, to determine the magnitudes of the different forces which produce the noise as well as of the different torques which influence the torque-speed curve. An exact knowledge of the currents produced by the field harmonics in the rotor is necessary for solving this problem. Since the magnitude of the harmonic currents in the rotor depends on the rotor leakage, a special study of the leakage of the single harmonics will be necessary. Of great influence is here the differential leakage; its amount increases with the order of the harmonic and therefore becomes important for the slot harmonics.

Thus a complete representation of the harmonic problem in induction motors involves the following topics:

1. The harmonics of the stator, their amplitudes, and their speed with regard to the stator and rotor.
2. The harmonics of the rotor, their amplitudes, and their speed with regard to the stator and rotor.
3. The force waves produced by the harmonics and their frequencies.
4. The resistance and reactance of the rotor with regard to the different harmonics.
5. The rotor currents produced by the different harmonics.
6. The forces produced by the harmonics.
7. The asynchronous torques.
8. The synchronous torques during running and at standstill.
9. Means to avoid the noise and the parasitic torques.

It is intended to divide the representation in three parts. This paper contains the topics 1, 2, and 3, that is, it treats the noise problem qualitatively. The magnitude of the forces that produce the noise will be determined in the third part, when the resistance and reactance of the rotor with regard to the different harmonics have been discussed in the second part.

II. The Stator and Rotor Harmonics

Since not only the windings with an integral number of slots per pole per phase are considered, but the windings with a fractional number of slots as well, it becomes expedient to take as the "fundamental" harmonic the wave, the length of which is equal to the circumference of the stator bore. The synchronous wave will be then the harmonic of the order $n' = p$; this is the "main" or "synchronous" harmonic. If in the case when the number of slots per pole per phase is an integer, it is desirable to use the main harmonic as the fundamental, the ratio $n = n'/p$ is valid; n is then the order of the harmonic with regard to the synchronous wave.

As shown in appendix 1, the order as well as the direction of rotation of the stator harmonics is defined by the two equations

$$\left(\frac{n'}{p}+1\right)\frac{\beta}{2}=k \tag{1}$$

$$\left(\frac{n'}{p}\mp 1\right)\frac{\beta}{2m_1}=k_1 \tag{2}$$

where k is an integer excluding 0, k_1 an integer including 0. When the minus sign in equation 2 is used, n' travels with the synchronous harmonic; when the plus sign in equation 2 is used, n' travels opposite to the synchronous harmonic.

When the number of slots per pole per phase q_1 is an integer, β has to be set in equations 1 and 2 equal to 2, whence

$$n'=(k_1m_1+1)P \tag{3}$$

or

$$n=k_1m_1+1 \tag{4}$$

where k_1 is any positive or negative integer including 0. When q_1 is an integer, and the winding is a symmetrical three-phase 60-degree phase belt winding, k_1 is a positive or negative even integer including 0. When n' is positive, the harmonic travels with the synchronous harmonic; when n' is negative, the harmonic travels opposite to the synchronous harmonic.

In appendix 1, it is further shown that the n' harmonic of the stator produces the following rotor harmonics

$$m'=k_2Q_2+n' \text{ for the squirrel-cage rotor} \tag{5}$$

$$m'=k_2pm_2+n' \text{ for the wound rotor with } q_2 \text{ an integer} \tag{6}$$

where k_2 is a positive as well as a negative integer including 0. For positive values of the slip s_n' the harmonic m' travels with the rotor when m' is positive, opposite to the rotor when m' is negative.

The stator slot harmonics are given by the equation

$$n_{s1}'=\pm Q_1+p \tag{7}$$

and the rotor slot harmonics by the equation

$$m_{s1}'=\pm Q_2+p \tag{8}$$

It follows from equations 5, 6, and 8 that the rotor slot harmonics are produced by the main (synchronous) wave of the stator, and that $k_2 = \pm 1$ corresponds to the slot harmonics of the squirrel-cage rotor, while a large k_2 corresponds to the slot harmonics of the wound rotor.

Equation 6 assumes that the number of slots per pole per phase q_2 of the rotor is an integer. If q_2 is a fractional number, $k_2\dfrac{2}{\beta_2}pm_2$ has to be inserted in equation 6 instead of k_2pm_2 (corresponding to equation 34, appendix 1). This is valid also for the formulas derived from equation 6 in the following.

In appendix 2 formulas are given for the harmonics of the magnetomotive forces of the stator and rotor which take into account their speed relative to the stator as well as to the rotor. Since the noise problem only is considered here, the magnetomotive forces with regard to the stator are of importance.

The magnetomotive force of the n'th stator harmonic is

$$b_{n'}=B_{n'}\sin\left(\omega t-n'\frac{\pi}{p\tau}x_1\right) \tag{9}$$

and the magnetomotive force of the m'th rotor harmonic

$$b_{m'}=B_{m'}\sin\left[\left(1+\frac{m'-n'}{p}(1-s)\right)\times \omega t-m'\frac{\pi}{p\tau}x_1\right] \tag{10}$$

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* The fundamental paper on the influence of harmonics in the squirrel cage rotor has been published by L. Dreyfus.[16] The most detailed representation is given in the textbook of R. Richter, ELEKTRISCHE MASCHINEN.[10]

The rotor harmonic m' is produced by the stator harmonic n'; therefore, corresponding to the equations 5 and 6

$$b_{m'}=B_{m'}\sin\left[\left(1+\frac{k_2Q_2}{p}(1-s)\right)\times \omega t-m'\frac{\pi}{p\tau}x_1\right] \text{for the squirrel-cage rotor} \quad (10a)$$

$$b_{m'}=B_{m'}\sin\left[(1+k_2m_2(1-s))\omega t-m'\frac{\pi}{p\tau}x_1\right] \text{for the wound rotor} \quad (10b)$$

The values of $B_{n'}$ and $B_{m'}$ are given in appendix 2.

III. The Force Waves and Their Frequencies

The driving force of the machine is a tangential force produced by the main flux wave and the main magnetomotive force wave. The flux harmonics and the harmonics of the magnetomotive force produce parasitic tangential forces which contribute to the noise. However, the main source of noise is the group of radial forces produced by the flux harmonics. The radial force that corresponds to the induction b in the gap is

$f_r=1.385b^2\times10^{-8}$ pounds per square inch

b consists of all stator and rotor harmonics. Hence b^2 is the sum of the squares of all stator and rotor harmonics plus twice the product of each stator harmonic with each other stator harmonic, of each rotor harmonic with each other rotor harmonic and of each stator harmonic with each rotor harmonic. We write in general form

$$f_r\equiv b_{\gamma'}b_{\mu'}$$

We can use this formula for all five components of b^2 making γ' and μ' equal to the values of n' and m' in consideration. It is in general

$$b_{\gamma'}=B_{\gamma'}\sin\left(\omega_{\gamma'}t-\gamma'\frac{\pi}{p\tau}x_1\right)$$

$$b_{\mu'}=B_{\mu'}\sin\left(\omega_{\mu'}t-\mu'\frac{\pi}{p\tau}x_1\right)$$

so that

$$f_r\equiv b_{\gamma'}\,b_{\mu'}\equiv\frac{1}{2}B'B_{\mu'}\times \cos\left[(\omega_{\gamma'}-\omega_{\mu'})t-(\gamma'-\mu')\frac{\pi}{p\tau}x_1\right]- \frac{1}{2}B_{\gamma'}B_{\mu'}\cos\left[(\omega_{\gamma'}+\omega_{\mu'})t-(\gamma'+\mu')\frac{\pi}{p\tau}x_1\right] \quad (11)$$

Thus the radial force f_r consists of two force waves, one having $\gamma'-\mu'$ force pole pairs and the frequency $\omega_{\gamma'}-\omega_{\mu}$ the other having $\gamma'+\mu'$ force pole pairs and the frequency $\omega_{\gamma'}+\omega_{\mu'}$. We designate the number of force pole pairs by k. $k=0$ is a stationary pulsating force, $k=1$ is a force wave with two poles, $k=2$ is a force wave with four poles, and so on. The larger the value of k, the shorter is the wave length. The stator is, in general, stiffer to short wave distortion (k large) than to large wave distortion (k small). Yet, depending on the construction of the machine, the small wave distortion may be dangerous as well.

We consider now the different components of b^2.

(*a*). *Stator harmonics only.* In equation 11 we have to insert $\gamma'=n_a'$, $\mu'=n_b'$

It follows further from equation 9

$$\omega_{\gamma'}=\omega_{\mu'}=\omega=2\pi f_1$$

Thus

$$k=n_a'+n_b' \qquad f_+=2f_1$$
$$k=n_a'-n_b' \qquad f_-=0 \quad (12)$$

f_+ is the frequency of the force wave, when the sum of n_a' and n_b' is taken; f_- is the frequency of the force wave, when the difference of n_a' and n_b' is taken. n_a' and n_b' can be positive as well as negative.

When a single stator wave is considered, then $n_b'=n_a'$ and

$$k=2n_a' \qquad f_+=2f_1 \quad (12a)$$

Thus the force waves produced by the stator harmonics have double the line frequency. Also the main harmonic produces a force wave of double the line frequency.

The consideration of the case $k=1$, which must be avoided, we limit for the sake of clearness to integral values of q_1, since fractional numbers of q_1 are not often used in induction motors. $k=1$ means

$$n_a'+n_b'=\pm1 \text{ or } n_a'-n_b'=\pm1$$

With equation 3 this gives

$$n_a'+n_b'=[(k_{1a}+k_{1b})m_1+2]p=\pm1$$
$$n_a'-n_b'=(k_{1a}-k_{1b})m_1p=\pm1$$

Since k is a positive or a negative integer including 0, the case $n_a'+n_b'=\pm1$ cannot occur. $n_a'-n_b'=\pm1$ is possible only when $p=1$, $m_1=1$ and k_{1a} or k_{1b} is an odd number, that is, when the magnetomotive force contains even harmonics.

(*b*). *Rotor harmonics only.* In this case $\gamma=m_a'$, $\mu=m_b'$. It follows further from equation 10

$$b_{m_a'}=B_{mba'}\sin\left[\left(1+\frac{m_a'-n_a'}{p}(1-s)\right)\times \omega t-m_a'\frac{\pi}{p\tau}x_1\right]$$

$$b_{m_b'}=B_{m_b'}\sin\left[\left(1+\frac{m_b'-n_b'}{p}(1-s)\right)\times \omega t-m_b'\frac{\pi}{p\tau}x_1\right]$$

Inserting here equation 6, we find for the wound rotor with q_2 integral

$$k=m_a'+m_b' \qquad f_+=[2+(k_{2a}+k_{2b})\times m_2(1-s)]f_1 \quad (13)$$

$$k=m_a'-m_b' \qquad f_-=[(k_{2a}-k_{2b})m_2(1-s)]f_1$$

and inserting equation 5, we find for the squirrel-cage rotor

$$k=m_a'+m_b' \qquad f_+=\left[2+(k_{2a}+k_{2b})\frac{Q_2}{p}(1-s)\right]f_1$$

$$k=m_a'-m_b' \qquad f_-=\left[(k_{2a}-k_{2b})\frac{Q_2}{p}(1-s)\right]f_1 \quad (14)$$

m_a', m_b', k_{2a}, and k_{2b} in equations 13 and 14 can be positive as well as negative.

For the case of the squirrel-cage motor as shown in appendix 2, the value of $B_{m'}$ becomes smaller, the larger the value of k_2. $k_2=\pm1$ gives the largest amplitude for the rotor harmonic. Considering this case, that is,

$$k_{2a}=\pm1 \qquad k_{2b}=\pm1$$

we find for the squirrel-cage rotor

$$f_+=\left[1\pm\frac{Q_2}{p}(1-s)\right]2f_1$$
$$f_-=0 \quad (13a)$$

when k_{2a} and k_{2b} have the same sign and

$$f_+=2f_1$$
$$f_-=\frac{Q_2}{p}(1-s)2f_1 \quad (13b)$$

when k_{2a} and k_{2b} have different signs.

When a single harmonic is considered, then $m_a'=m_b'$; $k_{2a}=k_{2b}=k_2$

$$k=2\mu_a' \qquad f=\left[1+k_2\frac{Q_2}{p}(1-s)\right]2f_1 \quad (13c)$$

m_2 instead of Q_2/p is to be inserted in this equation for the wound rotor.

If we limit again the consideration of the case $k=1$ to q_1 integral there will be

$$m_a'+m_b'=\pm1$$

or

$$m_a'-m_b'=\pm1$$

With equations 3 and 5, this gives for the squirrel-cage rotor

$$m_a'+m_b'=\pm1=(k_{2a}+k_{2b})Q_2+[(k_{1a}+k_{1b})m_1+2]p$$

k_{2a} and k_{2b} are positive or negative integers including 0. In the usual case of a 60-degree-phase-belt winding, k_{1a} and k_{1b} are even integers including 0. Only the 120-degree-phase-belt winding, (two speed motors) may have even values as well as odd values of k_1. In order that $m_a'+m_b'=\pm 1$, the equation must be satisfied.

$$(k_{2a}+k_{2b})Q_2=-(k_{1a}+k_{1b})m_1p-2p\mp 1$$

With k_{1a} and k_{1b} even integers, this is possible only when Q_2 is an odd number. For $m_a'-m_b'=\pm 1$ it follows that

$$m_a'-m_b'=\pm 1=(k_{2a}-k_{2b})Q_2+(k_{1a}-k_{1b})m_1p$$

or

$$(k_{2a}-k_{2b})Q_2=(k_{1a}-k_{1b})m_1p\mp 1$$

With k_{1a} and k_{1b} even integers this equation can be satisfied when Q_2 is an odd number. Thus the rotor harmonics produce $k=1$ (two-pole force waves) when Q_2 is an odd number. When the stator winding produces even harmonics (120-degree-phase-belt winding) also an even Q_2 may produce $k=1$. The wound rotor has usually q_2=integer and Q_2 an even number. In the case q_2=fractional number, the equation 3 and equation 34, appendix 1, have to be used in order to find whether or not $k=1$ is possible.

The value $\dfrac{Qn}{60}=\dfrac{Qf}{p}$ is the slot frequency. It follows from equations 13*a* and 13*b* that the frequency of the noise which the rotor harmonics produce is approximately equal to double the slot frequency or to double the line frequency. Seldom another frequency corresponding to equation 13 may occur.

(*c*). *Products of stator and rotor harmonics.* Here

$$\gamma'=n_b' \qquad \mu'=m_a'$$

m_a' is produced by the stator harmonic n_a'. From equations 9 and 10 we get for the squirrel-cage rotor

$$\left.\begin{aligned} k&=n_b'+m_a' & f_+&=\left[2+K_{2a}\frac{Q_2}{p}(1-s)\right]f_1\\ k&=n_b'-m_a' & f_-&=\left[K_{2a}\frac{Q_2}{p}(1-s)\right]f_1\end{aligned}\right\}\quad(15)$$

In this equation m_2 is to be substituted for $\dfrac{Q_2}{p}$ for the wound rotor with q_2 equal to an integer.

It follows from equation 15 that the frequency of the force waves produced by stator harmonics in co-operation with rotor harmonics does not depend on the order of the stator harmonic; it depends on the factor k_2 and also on the number of rotor phases, if the rotor is a wound rotor, or on the number of rotor slots, if the rotor is of squirrel-cage type.

When $k_{2a}=0$, m_a' is equal to n_a' and there will be

$$\left.\begin{aligned} k&=n_b'+m_a' & f_+&=2f_1\\ k&=n_b'-m_a' & f_-&=0\end{aligned}\right\}\quad(15a)$$

For the rotor slot harmonics of the squirrel cage, $k_{2a}=\pm 1$. Thus, when the rotor slot harmonics are involved, the frequencies of the force waves of the squirrel-cage rotor are

$$\left.\begin{aligned} k&=n_b'+m_a' & f_+&=\left[2\pm\frac{Q_2}{p}(1-s)\right]f_1\\ k&=n_b'-m_a' & f_-&=\frac{Q_2}{p}(1-s)f_1\end{aligned}\right\}\quad(15b)$$

It follows from equations 7 and 8 for the co-operation of the stator slot harmonics with the rotor slot harmonics

$$k_{s1}=n_{s1}'+m_{s1}'=\pm Q_1+p\pm Q_2+p$$

and

$$k_{s1}=n_{s1}'-m_{s1}'=\pm Q_1\mp Q_2$$

The smallest absolute values of k_{s1} are

$$\begin{aligned} k_{s1}&=Q_1-Q_2\pm 2p\\ k_{s1}&=Q_1-Q_2\end{aligned}\qquad(16)$$

Disturbing noise can be expected, when k_{s1} as given by equation 16 is a small number. $k_{s1}=1$ is dangerous, but under certain conditions even $k_{s1}=20$ may not be permissible. The noise produced by the machine depends not only on the harmonics and the radial forces produced by them, it depends also on the mechanical construction of the machine and on the number of poles which determines the depth of the stator core.

When the stator harmonics co-operate with rotor harmonics, the case $k=1$ results in

$$n_b'+m_a'=\pm 1$$

or

$$n_b'-m_a'=\pm 1$$

With equations 3 and 5, this gives for the squirrel-cage rotor (q_1 is an integer)

$$n_b'+m_a'=\pm 1=[(k_{1a}+k_{1b})m_1+2]p+k_{2a}Q_2$$

$$n_b'-m_a'=\pm 1=(k_{1b}-k_{1a})m_1p-k_{2a}Q_2$$

Applying here the same considerations as in the case of two rotor harmonics, treated under (*b*), we find the same results as there, namely that $k=1$ occurs when Q_2 is an odd number.

It follows from equations 15*a* and 15*b* that the frequency of the force waves of a squirrel-cage motor which are produced through the co-operation of the stator and rotor harmonics have approximately the slot frequency or double the line frequency. Occasionally higher frequencies corresponding to equation 15 may occur.

Since equation 8 is valid also for the wound rotor, the results derived for the squirrel-cage motor are valid also for the wound-rotor motor.

(*d*). *Zero-pole forces* ($k=0$). Under special circumstances the case $k=0$ may cause considerable trouble. $k=0$ means that there are no force poles, that the force is constant around the stator bore at any instant, but that it changes its value with the frequency $\omega_{\gamma'}-\omega_{\mu'}$ or $\omega_{\gamma'}+\omega_{\mu'}$ (equation 11). It can be seen from this equation and from the considerations under (*a*) and (*b*) that $k=0$ is possible only when stator harmonics co-operate with rotor harmonics; then we have $\omega_{\gamma'}-\omega_{\mu'}\neq 0$ and also $\omega_{\gamma'}+\omega_{\mu'}\neq 0$

Referring to (*c*) we have

$$\gamma'=n_b' \qquad \mu'=m_a'$$

and from equations 3 and 6 for the wound rotor

$$k=n_b'+m_a'=0=(k_{1b}+k_{1a})m_1+2+k_{2a}m_2$$

$$k=n_b'-m_a'=0=(k_{1b}-k_{1a})m_1-k_{2a}m_2$$

The number of phases of the rotor m_2 is usually equal to three. Thus $k=n_b'+m_a'$ will be equal to 0, when

$$(k_{1b}+k_{1a})\frac{m_1}{3}+\frac{2}{3}=-k_{2a}$$

that is, the sum $n_b'+m_a'$ cannot produce $k=0$, if the stator has $m_1=3$. $k=n_b'-m_a'$ will be equal to 0, when

$$(k_{1b}-k_{1a})\frac{m_1}{3}=k_{2a}\qquad(17a)$$

If the stator has a three-phase winding there will be in this case, many combinations which will give $k=0$. The frequency of the force is given by equation 15 when m_2 is substituted for Q_2/p. The equation 15 is independent of the value of k.

We consider for example, the combination (q_1 integral)

$$m_1=m_2=3 \quad k_{1a}=0 \quad k_{1b}=-2=k_{2a}$$

To $k_{1a}=0$ corresponds $n_a'=p$; to $k_{1b}=-2$ corresponds $n_b'=-5p$; to $k_{2a}=-2$ corresponds $m_a'=-6p+p=-5p$ and $n_b'-m_a'=0$. The frequency of the stationary force produced by $n_b'=-5p$ and $m_a'=-5p$ is (equation 15)

$$f_-=-2\times 3(1-s)f_1=360(1-s) \text{ cycles per second}$$

If we would consider the combination $k_{1a}=0$, $k_{1b}=-4=k_{2a}$, that is, $n_a'=p$, $n_b'=-11p$, $m_a'=-11p$, we would find that

the stationary force oscillates with 720 $(1-s)$ cycles per second.

For the squirrel-cage rotor we find from equations 3 and 5

$$k=n_b'+m_a'=0=[(k_{1b}+k_{1a})m_1+2]p+k_{2a}Q$$

$$k=n_b'-m_a'=0=(k_{1b}-k_{1a})m_1p-k_{2a}Q_2$$

Since k_{1a} and k_{1b} are integers, $k=0$ will occur here when

$$\left.\begin{array}{l} \dfrac{k_{2a}Q_2}{p} \text{ is an integer or} \\[2ex] \dfrac{k_{2a}Q_2}{m_1p} \text{ is an integer} \end{array}\right\} \quad (17b)$$

The frequency of the forces is given by equation 15 which is independent of the value of k.

The equation 17a for the wound rotor can be satisfied by small values of k_{1a}, k_{1b}, and k_{2a}. The corresponding field amplitudes as well as the $k=0$ force will therefore be considerable. To the equation 17b corresponds usually a relatively large value of k_{2a} or k_{1b} or k_{1a}, that is, a harmonic with small amplitude. This is the reason why $k=0$ may more often make trouble in a machine with a wound rotor than in a machine with a squirrel-cage rotor.

The natural frequency of a stator core for radial vibrations is approximately

$$f_{\text{rad. vib.}}=\frac{32.4\times10^3}{r}\sqrt{\frac{1}{1+h_t/h_c}} \text{ cycles per second} \quad (18)$$

where r is the radius in the middle of the core, h_t the depth of the tooth and h_c the depth of the core. It depends mainly on the radius of the core.

Influence of Saturation

For the stator harmonics (equation 37, appendix 2) is valid.

$$b_{n'}=B_{n'}\sin\left(\omega t-n'\frac{\pi}{p\tau}x_1\right)$$

Since all harmonics n' are produced by the same current, ω is common for all harmonics, and each harmonic travels exactly its wave length during one cycle of the current.

If the magnetic circuit is saturated, the main harmonic of the magnetomotive force produces, besides the main field harmonic, also saturation field harmonics which travel 3, 5, ... wave lengths when the main harmonic travels only one wave length. The saturation harmonics are bound to the main harmonic and travel with it without changing position with regard to each other. For these harmonics we have to write

$$b_\gamma=B_\gamma\sin\left(\gamma\omega t-\gamma\frac{\pi}{\tau}x_1\right) \quad \gamma=3,5,\ldots. \quad (19)$$

The saturation harmonics produce in cooperation with the rotor harmonics, force waves, the frequencies of which can be found from equations 11, 10, and 18 as follows

$$\left.\begin{array}{l}
\left.\begin{array}{l}\gamma_b'=3 \quad f_+=\left[4+\dfrac{m_a'-n_a'}{p}(1-s)\right]f_1 \\ k=\gamma_b'+m_a' \\ \gamma_b'=5 \quad f_+=\left[6+\dfrac{m_a'-n_a'}{p}(1-s)\right]f_1\end{array}\right. \\[4ex]
\left.\begin{array}{l}\gamma_b'=3 \quad f_-=\left[2-\dfrac{m_a'-n_a'}{p}(1-s)\right]f_1 \\ k=\gamma_b'-m_a' \\ \gamma_b'=5 \quad f_-=\left[4-\dfrac{m_a'-n_a'}{p}(1-s)\right]f_1\end{array}\right.
\end{array}\right\} \quad (20)$$

The difference $m_a'-n_a'$ is given for the wound rotor as well as for the squirrel-cage rotor, by equations 5, 6, and equation 34, appendix 1.

Appendix I. The Order of the Stator and Rotor Harmonics

(a) The Stator Harmonics

We consider a stator winding with a fractional number of slots per pole per phase. Since subharmonics are possible, we take as fundamental the wave, the length of which is equal to the complete developed armature, that is, to 2π. The harmonic $n'=p$ is then the main harmonic (the "synchronous" harmonic).

The stator may have m_1 phases. The n'th harmonic of the magnetomotive force of one of the phases which we designate by 0 is

$$f_{1n'}{}^0=F_{1n'}\sin\omega t\cos n'\frac{x_1}{p\tau}\pi \quad (21)$$

When the number of slots per pole per phase is

$$q_1=a+\frac{b}{\beta} \quad (22)$$

where a is an integer, b/β a fractional number, and b and β have not a common divisor, then the winding repeats itself after each β poles. The winding with $q_1=$integer repeats itself after two poles. The time and space angle between two adjacent phases are for q_1 an integer $\dfrac{2\pi}{m_1}$ and $n'\dfrac{2\pi}{pm_1}$; correspondingly these angles are for q_1 a fractional number

$$\frac{\pi\beta}{m_1} \text{ and } n'\frac{\pi\beta}{pm_1}$$

The time and space angle between the phase designated by 0 and the cth phase are therefore for $q_1=$fractional number

$$\frac{\pi\beta}{m_1}c \text{ and } n'\frac{\pi\beta}{pm_1}c$$

Thus the magnetomotive force of the cth phase is

$$f_{1n'}{}^c=F_{1n'}\sin\left(\omega t-\frac{\pi}{m_1}\beta c\right)\times \cos\left(n'\frac{x_1}{p\tau}\pi-n'\frac{\pi}{pm_1}\beta c\right) \quad (23)$$

or

$$f_{1n'}{}^c=\frac{1}{2}F_{1n'}\left\{\sin\left[\left(\omega t-n'\frac{x_1}{p\tau}\pi\right)+\left(\frac{n'}{p}-1\right)\frac{\pi}{m_1}\beta c\right]+\sin\left[\left(\omega t+n'\frac{x_1}{p\tau}\pi\right)-\left(\frac{n'}{p}+1\right)\frac{\pi}{m_1}\beta c\right]\right\} \quad (23a)$$

In order to find the resultant magnetomotive force we have to take the sum of all m_1 phases, that is, from $c=0$ to $c=m_1-1$. Thus

$$f_{1n'}=\frac{1}{2}F_{n'}\left\{\left[\sin\left(\omega t-n'\frac{x_1}{p\tau}\pi\right)\times\sum_{c=0}^{c=m_1-1}\cos\left(\frac{n'}{p}-1\right)\frac{\pi}{m_1}\beta c+\cos\left(\omega t-n'\frac{x_1}{p\tau}\pi\right)\times\sum_{c=0}^{c=m_1-1}\sin\left(\frac{n'}{p}-1\right)\frac{\pi}{m_1}\beta c\right]+\left[\sin\left(\omega t+n'\frac{x_1}{p\tau}\pi\right)\times\sum_{c=0}^{c=m_1-1}\cos\left(\frac{n'}{p}+1\right)\frac{\pi}{m_1}\beta c-\cos\left(\omega t+n'\frac{x_1}{p\tau}\pi\right)\sum_{c=0}^{c=m_1-1}\sin\left(\frac{n'}{p}+1\right)\frac{\pi}{m_1}\beta c\right]\right\} \quad (24)$$

The four sums we designate in turn by a, b, a_1, and b_1, and the angles in the round parenthesis by α and δ. We can write

$$f_{1n'}=\frac{1}{2}F_{n'}[(a\sin\alpha+b\cos\alpha)+(a_1\sin\delta-b_1\cos\delta)] \quad (25)$$

or

$$f_{1n'}=\frac{1}{2}F_{n'}[\sqrt{a^2+b^2}\sin(\alpha+\gamma_1)+\sqrt{a_1^2+b_1^2}\sin(\delta+\gamma_2)]$$

$$=\frac{1}{2}F_{n'}\sqrt{a^2+b^2}\sin\left(\omega t-n'\frac{x_1}{p\tau}\pi+\gamma_1\right)+\frac{1}{2}F_{n'}\sqrt{a_1^2+b_1^2}\sin\left(\omega t+n'\frac{x_1}{p\tau}\pi+\gamma_2\right) \quad (25a)$$

These are two waves traveling in different directions, and both of the same order n'. Since the stator currents produce from each harmonic only one wave, traveling with the synchronous harmonic or opposite to it, it follows that for some harmonics the sums a and b, for other harmonics the sums a_1 and b_1, must be equal to zero.

We assume that a given harmonic, for example the n'th harmonic, travels with the synchronous harmonic. Then for this harmonic the sums a_1 and b_1 must be equal to zero, while $\sqrt{a^2+b^2}$ must have a definite

value. It can be seen that this will occur when

$$\left(\frac{n'}{p}+1\right)\frac{\beta}{2}=k \qquad k \text{ integer excluding } 0$$

and

$$\left(\frac{n'}{p}-1\right)\frac{\pi}{m_1}\beta=2\pi k_1 \qquad k_1 \text{ integer including } 0$$

There will be

$$a=\sum_{c=0}^{c=m_1-1}\cos\left(\frac{n'}{p}-1\right)\frac{\pi}{m_1}\beta c$$

$$=\sum_{c=0}^{c=m_1-1}\cos k_1 2\pi c=m_1$$

$$b=\sum_{c=0}^{c=m_1-1}\sin\left(\frac{n'}{p}-1\right)\frac{\pi}{m_1}\beta c$$

$$=\sum_{c=0}^{c=m_1-1}\sin k_1 2\pi c=0$$

$$a_1=\sum_{c=0}^{c=m_1-1}\cos\left(\frac{n'}{p}+1\right)\frac{\pi}{m_1}\beta c$$

$$=\sum_{c=0}^{c=m_1-1}\cos k\frac{2\pi}{m_1}c=0$$

$$b_1=\sum_{c=0}^{c=m_1-1}\sin\left(\frac{n'}{p}+1\right)\frac{\pi}{m_1}\beta c$$

$$=\sum_{c=0}^{c=m_1-1}\sin k\frac{2\pi}{m_1}c=0$$

On the other hand, if we assume that a given harmonic travels opposite to the rotation, we find that when

$$\left(\frac{n'}{p}+1\right)\frac{\beta}{2}=k \qquad k \text{ integer excluding } 0$$

$$\left(\frac{n'}{p}+1\right)\frac{\pi}{m_1}\beta=k_1 2\pi \qquad k_1 \text{ integer including } 0$$

the sums a and b will be zero while $\sqrt{a_1^2+b_1^2}$ has a definite value. Thus

$$\left.\begin{aligned}&\left(\frac{n'}{p}+1\right)\frac{\beta}{2}=k && k \text{ integer excluding } 0\\ &\left(\frac{n'}{p}\mp 1\right)\frac{\beta}{2m_1}=k_1 && k_1 \text{ integer including } 0\end{aligned}\right\} \quad (26)$$

are the criterions for the existence of the n'th harmonic in the magnetomotive force. When the minus sign is used, the harmonic travels with the synchronous harmonic; when the plus sign is used, the harmonic travels opposite to the synchronous harmonic.

The first criterion is independent of m_1, it indicates the existence of the n'th harmonic in the magnetomotive force of a single phase; the second criterion indicates the existence of the nth harmonic in the magnetomotive force of the total winding.[18]

It follows from the preceding that with $\beta=2$, the equations 26 are the criterions for the existence of the nth harmonic in a winding with q_1=integer. Thus, when q_1=integer,

$$\left.\begin{aligned}&\frac{n'}{p}+1=k && k=\text{integer excluding } 0\\ &\left(\frac{n'}{p}\mp 1\right)\frac{1}{m_1}=k_1 && k_1=\text{integer including } 0\end{aligned}\right\} \quad (27)$$

are the criterions for the existence of the n'th harmonic. Since n'/p is also an integer when q_1=integer, the first criterion says that the magnetomotive force of a single phase contains in general all odd as well as all even harmonics. The usually used 60-degree phase-belt winding does not contain even harmonics; for this winding k is limited to even integers.

The second criterion can be more conveniently used in the form

$$n'=(k_1m_1+1)p \tag{28}$$

where k_1 is a positive as well as a negative integer including 0. For the normal 60-degree phase-belt winding, k_1 is again limited to positive and negative even integers including 0.

(b) The Rotor Harmonics

The n'th stator harmonic induces in the rotor phase designated by 0 the current

$$i_{2n'}=\sqrt{2}\,I_{2n'}\sin s_{n'}\omega t$$

s_n' is the slip of the rotor relative to the n'th stator harmonic. To the current $i_{2n'}$ corresponds a rotor magnetomotive force, which contains different harmonics. We consider the m'th of these harmonics. Its magnetomotive force is

$$f_{2m'}=F_{2m'}\sin s_{n'}\omega t\cos m'\frac{x_2}{p\tau}\pi \tag{29}$$

We assume at first that the number of slots per pole per phase of the rotor is an integral number. Then the space angle between two adjacent phases is, as for the stator, $m'\frac{2\pi}{pm_2}$. The time angle between the electromotive forces of two adjacent rotor phases is determined by the n'th harmonic of the stator which produces the m' harmonic of the rotor, and since the space angle between two adjacent rotor phases is equal to $\frac{2\pi}{pm_2}$ this time angle is $n'\frac{2\pi}{pm_2}$. The time and space angle between the phase 0 and the cth phase are $n'\frac{2\pi}{pm_2}c$ and $m'\frac{2\pi}{pm_2}c$, and thus the resultant magnetomotive force

$$f_{2m'}=\frac{1}{2}F_{2m'}\left\{\left[\sin\left(s_{n'}\omega t-m'\frac{x_2}{p\tau}\pi\right)\times\sum_{c=0}^{c=m_2-1}\cos(m'-n')\frac{2\pi}{pm_2}c+\cos\left(s_{n'}\omega t-m'\frac{x_2}{p\tau}\pi\right)\sum_{c=0}^{c=m_2-1}\sin(m'-n')\frac{2\pi}{pm_2}c\right]+\left[\sin\left(s_{n'}\omega t+m'\frac{x_2}{p\tau}\pi\right)\sum_{c=0}^{c=m_2-1}\cos(m'+n')\times\frac{2\pi}{pm_2}c-\cos\left(s_{n'}\omega t+m'\frac{x_2}{p\tau}\pi\right)\times\sum_{c=0}^{c=m_2-1}\sin(m'+n')\frac{2\pi}{pm_2}c\right]\right\} \tag{30}$$

Since m' and n' are integers, the sums are equal to 0 except when

$$(m'-n')\frac{2\pi}{pm_2}=k_2 2\pi \qquad k_2 \text{ integer including } 0$$

and

$$(m'+n')\frac{2\pi}{pm_2}=k_2 2\pi \qquad k_2 \text{ integer including } 0$$

or except when

$$m'=k_2pm_2\pm n' \tag{31}$$

For a given value of k_2 to any value of n' correspond two different values of m', that is, two traveling waves. Giving k_2 positive as well as negative values including 0, we can write

$$m'=k_2pm_2+n' \qquad k_2 \text{ positive or negative integer including } 0 \tag{32}$$

n' can be here, as before, positive or negative.

For the squirrel-cage rotor $m_2=\frac{Q_2}{p}$, and therefore

$$m'=k_2Q_2+n' \tag{33}$$

While considering the wound rotor we have made the assumption q_2=integer. When q_2=fractional number it will be

$$m'=\frac{2}{\beta_2}k_2pm_2+n' \tag{34}$$

This equation can be found by a similar consideration as for the stator.

Appendix 2. The Amplitudes of the Stator and Rotor Harmonics and the Speed of the Harmonics Relative to the Stator and to the Rotor

(a) The Stator Harmonics

For the n'th harmonic of the stator magnetomotive force

$$_1, t)=F_{n'}\sin\left(\omega t-n'\frac{x_1}{p\tau}\pi\right) \tag{35}$$

is valid, where

$$F_{n'}=\frac{\sqrt{2}}{\pi}m_1N_1\frac{K_{dn'}K_{pn'}}{n'}I_1 \tag{36}$$

For the n'th harmonic of the field

$$b_{n'}(x_1, t)=B_{n'}\sin\left(\omega t-n'\frac{x_1}{p\tau}\pi\right) \tag{37}$$

where

$$B_{n'}=\frac{3.19}{gk_ck_s}\frac{\sqrt{2}}{\pi}m_1N_1\frac{K_{dn'}K_{pn'}}{n'}I_1 \tag{38}$$

k_c is the Carter factor. The factor k_s takes into account the saturation. It is

$$k_s=\frac{AT_g+AT_{t1}+AT_{t2}}{AT_g} \tag{39}$$

For the higher values of n', k_s is to be set equal to 1, since the paths of these harmonics are through the tooth tops without penetrating further into the teeth and the core.

The velocity of the n'th harmonic of the field with regard to the stator will be found

by differentiation with respect to t of the equation

$$\omega t - n'\frac{x_1}{p\tau}\pi = \text{constant}$$

as

$$v_{1n'} = \frac{dx_1}{dt} = \frac{p}{n'}\frac{\tau}{\pi}\omega = \frac{P}{n'}v_p \qquad (40)$$

where

$$v_p = \frac{\tau}{\pi}\omega = 2\tau f_1 \qquad (41)$$

is the velocity of the main (synchronous) harmonic ($n'=p$).

In order to determine the electromotive force induced by the n'th stator harmonic in the rotor winding, it is necessary to know the velocity of this harmonic with regard to the rotor.

If s is the slip of the rotor relative to the main harmonic, the velocity of the rotor is

$$v_r = (1-s)v_p = (1-s)\frac{\tau}{\pi}\omega$$

and therefore

$$x_2 = x_1 - v_r t = x_1 - (1-s)\frac{\tau}{\pi}\omega t$$

or

$$x_1 = x_2 + (1-s)\frac{\tau}{\pi}\omega t \qquad (42)$$

Inserting this in equation 37 there will be found

$$b_{n'}(x_2, t) = B_{n'}\sin\left(s_{n'}\omega t - n'\frac{x_2}{p\tau}\pi\right) \qquad (43)$$

where

$$s_{n'} = 1 - \frac{n'}{p}(1-s) \qquad (44)$$

is the slip of the rotor with regard to the n'th stator harmonic. It is also

$$s_{n'} = \frac{f_{2n'}}{f_1} \qquad (45)$$

$f_{2n'}$ is the frequency of the electromotive force induced by the n'th stator harmonic in the rotor winding. It follows from equation 43 for the velocity of the n'th stator harmonic with regard to the rotor

$$v_{2n'} = \frac{dx_2}{dt} = s_{n'}\frac{p}{n'}v_p = s_{n'}v_{1n'} \qquad (46)$$

It follows further from equation 40

$$(\text{Rpm})_{n'} = \frac{P}{n'}n_s = \frac{60f_1}{n'} \qquad (46a)$$

that is, the speed of a harmonic is inversely to its order. If there exists a fundamental ($n'=1$), its speed relative to the stator will be equal to $60f_1$, that is, 3,600 rpm for $f_1=60$ cycles per second. This is the highest speed that a stator harmonic can reach.

(b) The Rotor Harmonics

Corresponding to equation 37 we can write for the m'th harmonic of the rotor field

$$b_{m'}(x_2, t) = B_{m'}\sin\left(s_{n'}\omega t - m'\frac{\pi}{p\tau}x_2\right) \qquad (47)$$

This m'th rotor harmonic is produced by the n'th stator harmonic. The frequency of the rotor currents depends on the slip of the rotor relative to the n'th stator harmonic. For the amplitude $B_{m'}$ is valid (equation 4)

$$B_{m'} = \frac{3.19}{gk_ck_s}\frac{\sqrt{2}}{\pi}m_2N_2\frac{K_{dm'}K_{pm'}}{m'}I_{2n'} \qquad (48)$$

$I_{2n'}$ is the current induced in the rotor by the stator harmonic n'.

For the squirrel-cage rotor we have to insert in equation 48

$$m_2 = \frac{Q_2}{p},\quad N_2 = 1/2, \text{ and } K_{dm'} = K_{pm'} = 1$$

Further, we have to multiply equation 48 by p, if we understand $I_{2n'}$ to be the current per slot. Thus the field amplitude $B_{m'}$ of the squirrel-cage rotor is

$$B_{m'} = \frac{3.19}{gk_ck_s}\frac{\sqrt{2}}{2\pi}Q_2\frac{1}{m'}I_{2n'} \qquad (48a)$$

It follows from equation 33 (appendix 1) for the squirrel-cage rotor that its value of $B_{m'}$ is usually the larger the smaller is k_2. $k_2=\pm 1$ gives usually the largest value for $B_{m'}$.

From equations 47 and 44, it follows for the velocity of the m'th rotor harmonic with regard to the rotor

$$v_{2m'} = \frac{dx_2}{dt} = s_{n'}\frac{p}{m'}\frac{\tau}{\pi}\omega = \frac{p}{m'}\left[1 - \frac{n'}{p}\times (1-s)\right]v_p \qquad (49)$$

In order to study the noise problem it is necessary to know the velocity of the harmonics of the rotor fields with regard to the stator. Inserting in equation 47 the value of x_2 from equation 42

$$b_{m'}(x_1, t) = B_{m'}\sin\left[\left(1 + \frac{m'-n'}{p}(1-s)\right)\times \omega t - m'\frac{x'}{p\tau}\pi\right] \qquad (50)$$

The velocity of the m' rotor field harmonic with regard to the stator is thus

$$v_{1m'} = \frac{dx_1}{dt} = \frac{p}{m'}\left[1 + \frac{m'-n'}{p}(1-s)\right]v_p \qquad (51)$$

In the last two equations we have to substitute equations 32, 33, and 34 (appendix I)

$$\frac{m'-n'}{p} = k_2m_2 \quad \text{for the wound rotor with } q_2 = \text{integer}$$

$$\frac{m'-n'}{p} = \frac{2}{\beta_2}k_2m_2 \quad \text{for the wound rotor with } q_2 = \text{fractional number} \qquad (52)$$

$$\frac{m'-n'}{p} = k_2\frac{Q_2}{p} \quad \text{for the squirrel cage}$$

where k_2 is a positive or negative integer including 0.

We have seen that with $f_1=60$ cycles per second, the speed of the stator harmonics cannot exceed 3,600 rpm. The rotor harmonics are produced by currents of low as well as of high frequency. The speed that corresponds to the latter harmonics may be very high. It follows from equations 4 and 51

$$(\text{Rpm})_{2m'} = \frac{p}{m'}\left[1 - \frac{n'}{p}(1-s)\right]n_s \qquad (5$$

and

$$(\text{Rpm})_{1m'} = \frac{p}{m'}\left[1 + \frac{m'-n'}{p}(1-s)\right]n_s \qquad (5$$

We consider for example a squirrel-ca motor of 30 horsepower, four poles, cycles, three-phase, with $Q_1=72$, $Q_2=58$ a examine the combination $n'=+62$, $k_2=-$ $m'=+4$. It follows from equations 53 a 54 with $s=1.5$ per cent

$$(\text{Rpm})_{2m'} = \frac{1}{2}\left(1 - \frac{62}{2}\times 0.985\right)1{,}800 = 26{,}50$$

$$(\text{Rpm})_{1m'} = \frac{1}{2}\left(1 - \frac{58}{2}\times 0.985\right)1{,}800 = 24{,}80$$

The frequency of the rotor currents th correspond to the combination in consider tion is (equation 44 or 45)

$$f_{2n'} = \left(1 - \frac{62}{2}\times 0.985\right)60 = 1{,}768 \text{ cycles p second}$$

Nomenclature

- b—Instantaneous value of the field i tensity
- B—Amplitude of the field
- f—Instantaneous value of the magnet motive force
- f—Frequency
- f_1—Line frequency
- F—Amplitude of the magnetomotive for
- k—Integer
- k—Number of force pole pairs
- k_d—Distribution factor
- k_p—Pitch factor
- k_c—Carter factor
- k_s—Saturation factor
- m_1—Number of stator phases
- m_2—Number of rotor phases
- m'—Order of the rotor harmonics
- m_{s1}'—Order of the slot harmonics of t rotor
- n—Speed (rpm)
- n'—Order of the stator harmonic
- n_{s1}'—Order of the slot harmonics of t stator
- N—Number of turns per phase
- p—Number of pole pairs
- q—Number of slots per pole per phase
- Q—Total number of slots
- s—Slip
- v—Velocity
- β—Number of poles per group, wh q_1 = fractional number
- τ—Pole pitch
- ω—$2\pi f_1$

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Transient Recovery Voltages and Circuit-Breaker Performance

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EXPERIENCE with power circuit breakers has frequently shown that two breakers of identical design may give radically different performances, even though the operating voltages and short-circuit currents to which they are subjected are the same. Studies of the conditions surrounding these circuit breakers have traced the cause to the differences in the manner in which the recovery voltage has appeared across the contacts of the circuit breakers subsequent to the final interruption. This voltage which occurs between the final arc voltage and the 60-cycle recovery voltage is an equalizing phenomenon defined as the transient recovery voltage. To take it into account in breaker applications, studies have been made of the transient voltages which can occur on systems, and other studies have been made of the response of the circuit breakers to transient recovery voltages.

In the higher-voltage classes of circuit breakers, 34.5 kv and above, the voltage-recovery rates encountered in service are most severe when the circuit breaker is located adjacent to the power transformers. This is exactly the condition which is encountered in high-power laboratories, and so the recovery rates produced on the laboratory circuits for a given voltage and current closely approximate the maximum natural frequency and voltage-recovery rate which can be encountered in the service. In fact, service conditions will frequently involve the use of transmission lines supplying power to the circuit breaker. These lines may be supplying only a fraction of the total power which the breaker is called upon to interrupt, but the presence of the line on the bus greatly increases the capacitance of the circuit and reduces the rate at which the voltage appears across the breaker contacts. Consequently, when the voltages and currents can be duplicated on the laboratory high-voltage circuits, the transient recovery voltages obtained will probably be as severe or more severe than the transient recovery voltages encountered in service. A laboratory demonstration of the interrupting ability of a high-voltage breaker is, therefore, an excellent assurance of equally good service performance.

Studies of power-system circuits at generator voltage have revealed that the location of a reactor close to a circuit breaker produces a very high natural frequency in the transient recovery voltage. Some of these values have been found to be even higher than those encountered in normal high-power laboratory circuits. Very little was known about the performance of circuit breakers when subjected to these extremely high frequencies, and the need for additional data was apparent.

To eliminate this deficiency by studying transient recovery voltages having very high natural frequencies and to determine the effect of two-frequency transients, the tests described in this paper were made. These tests, made on four different types of circuit breakers, demonstrate the effects of various types of transient recovery voltages and show that the extremely high natural frequencies obtained with reactors close to breakers are not appreciably more severe on the circuit breakers than the natural frequencies normally obtained in the laboratory. They also reveal that tests made on the regular laboratory circuits may often be used to demonstrate the ability of the breaker to open under the most severe circuit conditions.

The Problem

Interrupting a circuit by a breaker consists of changing the current path through it from a good conductor to a good insulator. This is done preferably in as short a time as possible, with a minimum depreciation of breaker parts and as little external demonstration as possible. To accomplish this, the breaker separates the metallic current-carrying parts, thereby drawing an arc which temporarily completes the circuit. This current goes to zero periodically twice each cycle, and it is at one of these current zeros that the breaker completes the transition from a conductor to an insulator by deionizing the space between the contacts. An opportunity to do this occurs at each zero, but the arc will restrike if the voltage applied across the breaker exceeds the dielectric strength of the arc space. The dielectric strength of an ionized space has been defined by J. Slepian as the voltage required for sustaining or increasing the conductivity of the arc space, and it depends on the amount of ionization remaining and the rate at which the breaker is reducing it. The removal of the ionization requires time, and the breaker dielectric strength increases at a finite rate. This strength as defined above is finite at all times but increases toward the end of a half-cycle and just before current zero may attain high values. Unless at some time the applied voltage exceeds it, the dielectric strength will continue to increase after current zero, and the transition to an insulating state will be completed.

At the time of current zero in the

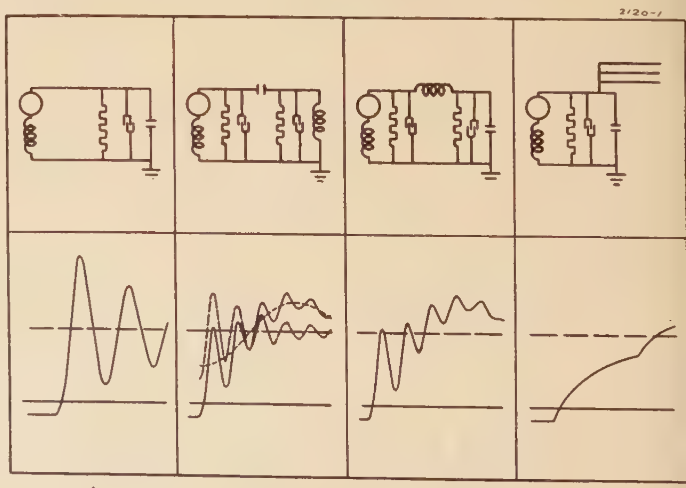

Figure 1. Typical simplified circuits and their corresponding transient recovery voltages

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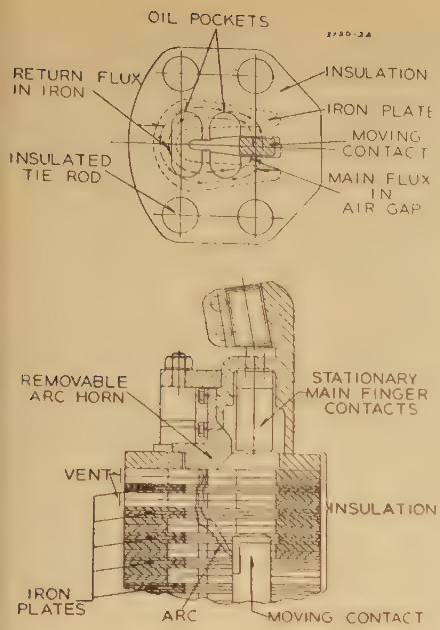
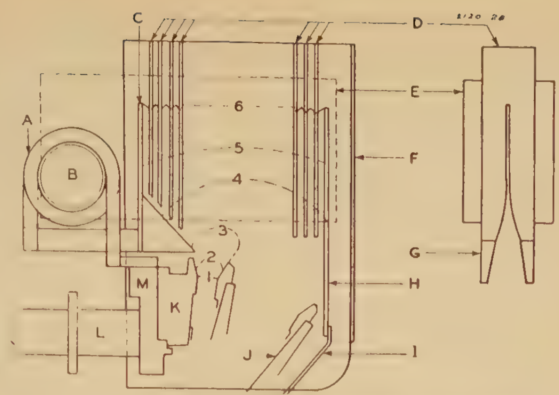


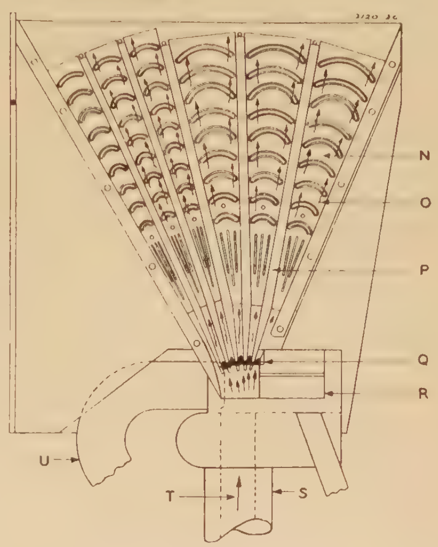
De-ion grid

Figure 2. Diagrammatic views of equipment used in these tests

Magnetic blowout air-breaker stack (right)

A—Magnet coil
B—Iron yoke
C—Panel-end horn
D—Insulation plates
E—Laminated iron shoes
F—Outline of arc chamber
G—Arc shield
H—Arcing horn
I—Shunt strap
J—Moving contact arm and arcing contact
K—Arcing and secondary contact platform
L—Micarta bushing
M—Upper stud





Compressed-air-breaker stack

N—Metal coolers
O—Exhaust gases
P—Insulation splitters
Q—Arc
R—Stationary contact
S—Tubular support
T—Air blast
U—Moving contact

eaker, the amount of ionization will metimes be negligible, and at other nes a sufficient amount may remain so at a current of a few amperes will flow rough the arc space when the transient covery voltage is applied across it.

The transient recovery voltage rises at rate determined by the ability of the rcuit to charge the capacitance adjant to the breaker. The voltage does ot increase at a constant rate nor always the same general manner. The charteristics of the transient are determined imarily by the circuit constants. A mple circuit will have a transient recovery voltage which is essentially a sinuidal wave as illustrated in Figure 1A. the circuit breaker separates two parts a circuit as shown in Figure 1B, ch part will oscillate independently, d recovery voltage on the circuit eaker will be a two-frequency transient, e relative frequencies and amplitudes epending upon the inductances and caicitances of the circuit. A similar ansient is produced by the more comicated circuit shown in Figure 1C. he addition of feeders to the bus, as own in Figure 1D, may produce transnts that are not sinusoidal.

These variations in the character of the ansients are, of course, accompanied variations in the speed with which ey take place, as the inductances, caicitances, and number of lines are varile. The natural frequencies of the rcuits used in these tests varied from 250 210,000 cycles per second.

The higher natural frequencies are used by a low value of capacitance adcent to the breaker, and only small curnts flow into these capacitances during the transient. Consequently, if the ionization remaining in the arc space permits a comparable current to flow through the breaker in parallel with the current through the capacitance, a considerable modification of the transient recovery voltage is to be expected and actually takes place, as will be shown by cathode-ray oscillograms.

The circuits of a high-power laboratory for testing the interrupting capacity of breakers have one or more generators, current-limiting reactors, closing switches, backup breakers, and the circuit breaker on test. For convenience, the reactors are permanently installed as a part of the laboratory equipment and are provided with suitable switching arrangements for varying the effective value of the reactance in the lines over a relatively wide range. Consequently, the capacitance of the bus and switches between the reactor and the circuit breaker may be greater than it would be in a power station having the current-limiting reactors located within a few feet of the circuit breaker. To determine how much effect this additional capacitance might have and to extend the range of tests up to as high values as could be obtained, reactors were placed in the test cells with the breakers and were connected to them by short leads.

Four types of breakers were studied. The first was an oil circuit breaker using plain-break-type contacts with no means of controlling or directing the gases generated in the oil. The second was also an oil breaker but was equipped with modern arc-rupturing devices ("De-ion grids"), Figure 2. It belonged to the class of breakers using self-generated gas blasts. The third was an air circuit breaker which produced a magnetic blast to drive the arc into a confining slot. The fourth type was the compressed-air circuit breaker which subjected the arc to a transverse blast of compressed air and forced it against arc splitters.

Because of the various transients which may be encountered, three different types of tests were used.

In one type of test, the single-frequency transients, illustrated in Figure 1A, were used with the voltage and current constant and the capacitance varied. These tests demonstrated the effect of varying the natural frequency of the transient over a very wide range.

Another type of test covered a wide range of currents at a given voltage. Two series of tests were made, differing only in the natural frequency of the transient recovery voltage. Due to the inductance varying with current and the limitations within which the capacitances of the laboratory could be controlled, it

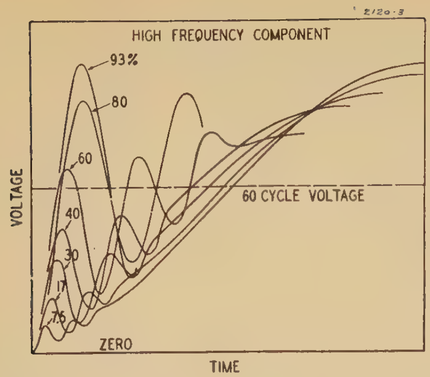


Figure 3. Calculated transient voltages for circuits used to determine the effect of varying the amplitude of the high-frequency component

was not possible to maintain the same natural frequency for all of the current settings or even to keep them all of the same general type. Consequently, for comparison, they were all based on the average rate of increase in voltage to the first peak, the transient voltage-recovery rate. This value was about 2,000 volts per microsecond for one group and 3,000 volts per microsecond for the other.

In the third type of test, voltage-recovery transients composed of two components having different frequencies were studied (Figure 1C). The tests were made at a given voltage and current, and, by varying the distribution of the impedance in the circuit, the amplitude of the high frequency component was varied from 7.5 to 93 per cent of the total amplitude of the transient (Figure 3). This was done to determine how high the first peak of voltage had to be before it exerted an influence on the performance of the circuit breaker. If the amplitude of the higher frequency component is relatively small, the voltages reached during the part of the transient controlled by it may be so low that they exert little or no influence on the breaker performance. However, if the amplitude of the higher-frequency component is relatively high, the transient approximates a single-frequency transient and naturally produces practically the same effect.

All tests were made on single-phase 60-cycle circuits. The 7,620-volt circuits had one side of the breaker grounded. The 13,200-volt circuits had the neutral of the generator grounded through a resistor. Records were made on a magnetic oscillograph and on a cathode-ray oscillograph having a rotating film drum. To expedite the testing, records of two similar tests frequently were made on one cathode-ray-oscillograph film using two different time scales.

Plain-Break Oil Circuit Breaker

The tests on the oil circuit breaker not equipped with arc-extinguishing devices demonstrated its performance over a wide range of circuit voltage-recovery rates. The breaker used had a tank diameter of 30 inches, a contact stroke of $11^1/_8$ inches and an average contact opening speed of 8.2 feet per second. The two breaks were each ten inches long. All tests were single-phase with one side grounded at the breaker. The tests were close to the voltage-interrupting ability but were well below the current-interrupting ability of the breaker.

A series of tests was made to determine the effect of varying the natural frequency of the circuit. This was done on a 7,620-volt 5,200-ampere circuit but at a potential of about 3,810 volts and a current of 2,600 amperes. A wide range of natural frequencies was obtained. The highest frequency, 208,000 cycles per second, resulted from placing a 1.2-ohm reactor in the test cell immediately adjacent to the circuit breaker. The lower values, down to 380 cycles per second, were obtained by various laboratory circuit connections and by adding capacitance to the normal laboratory circuit. The arcing time was a function of the natural frequency as shown by Figure 4.

The cathode-ray oscillograms, Figure 5, show that at the higher frequencies the oscillations of the transients were completely damped, and the damping became relatively less at lower frequencies so that the maximum voltage which was reached during the extinction transients varied as shown in Figure 4. The voltage reached by the transient a half-cycle before interruption was also measured and plotted in Figure 4. At the higher frequencies, these voltages were well below the voltages reached on the final transient. However, as the frequency was reduced, and the energy of the transient was increased, the voltage on the transients increased and approached double the normal crest of the sixty-cycle wave. As this approximates the normal maximum voltage of a recovery transient, further reduction in frequency permitted the arc to extinguish on this transient which then became the final transient. The group of crosses representing maximum voltages on final transient recovery voltages for 500 to 600 cycles per second can be considered an indication of the end of this curve.

An interesting discontinuity exists in the arcing time curve at about this frequency. The breaker seemed to make a good effort to extinguish the arc within the first cycle and, if it failed to do so would be unable to accomplish it until about the end of the third cycle of arcing. Below 400 cycles per second the breaker interrupted with about one-half cycle of arcing.

At six of the steps the maximum voltage which could be consistently interrupted was determined and was found to be about 3,810 volts 2,600 amperes. At higher voltages occasional failures occurred, and this limit appeared to be independent of the natural frequency of the circuit.

Two other series of tests were made to determine the effect of the amplitude of the higher-frequency component of two frequency transients. They were planned and made prior to the series described above and before it was known that this breaker had a constant arcing time over such a wide range of natural frequencies. The lower frequencies chosen were as low as could be easily obtained but varied from 2,700 to 8,000 cycles per second on the low-current series and from 7,000 to 17,000 cycles per second on the higher current series. These series gave dat

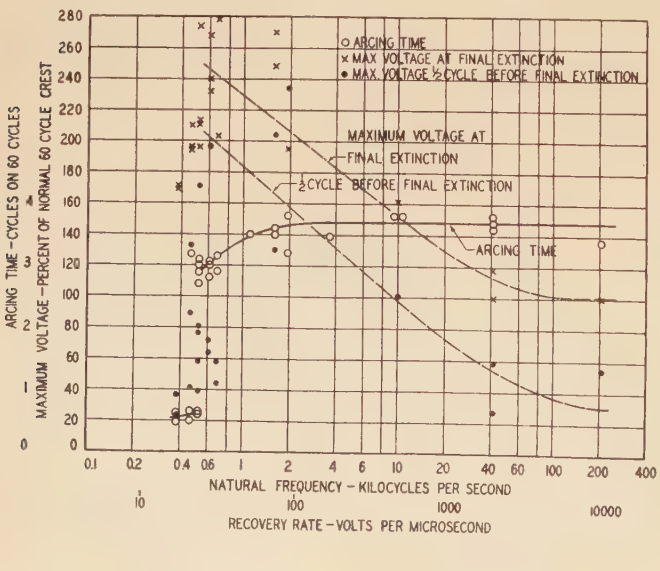
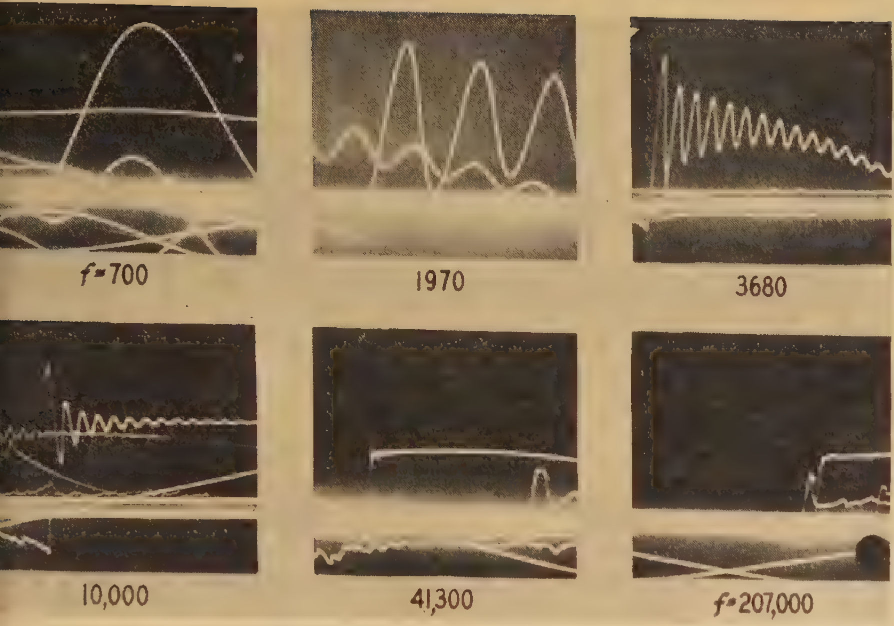


Figure 4. Plain-break circuit breaker, 3,800 volts, 2,600 amperes

Effect of natural frequency of transient recovery voltage



igure 5. Plain-break circuit breaker, 3,800 volts, 2,600 amperes

Cathode-ray oscillograms showing effect of onductivity in the arc space on the transient recovery voltage

vhich indicated that at both currents the rcing time was substantially independ-nt of the amplitude of the higher-fre-uency component, and this result was lifficult to explain until the effect of vary-ng the single-frequency transients over nuch wider ranges was disclosed by the ater test. Consequently, the data on hese tests are of interest in supplement-ng the previous data and in showing the ffect of the conductivity of the arc pace on the damping of the double-requency transients.

In one series of tests, made with a cur-ent of 1,400 amperes at 3,180 volts, the mplitude of high-frequency component vas varied from 7.5 to 93 per cent of the otal amplitude in about ten per cent teps. The transients calculated during he planning of the test are shown in 'igure 3, and the transients actually ob-ained are shown in Figure 6. The ac-ual transients were modified consider-bly by the current being passed through he arc space of the circuit breaker.

The transients having calculated high-requency components of 80 and 90 per ent were so heavily damped by the cur-ent through the arc space that they ap-eared aperiodic. Data from these tests re plotted in Figure 7 as a function of he amplitude of the calculated high-requency component. The arcing time emained substantially constant. The naximum peak voltage decreased almost nearly, indicating that the higher-fre-uency component had negligible amplitude at the time of the maximum crest. The first peak increased almost linearly with the amplitude but at a rate only about half as great as would have been reached without conductivity in the arc space.

A similar series of tests were made at higher current, 6,000 amperes at 3,050 volts with high-frequency components having from 7 to 75 per cent of the total amplitude. For these values also the arcing time was independent of the amplitude of the high-frequency component, Figure 8. The conductivity was sufficient in all cases to damp completely the high-frequency component, and in some cases, the low-frequency component also. The low-frequency components increased in frequency as the amplitude decreased, but the range was only from 7,000 to 17,000 cycles per second. The constant arcing time and the damping of the transients indicates that the breaker easily dissipated all of the energy of the high-frequency component and that arcing times were the same for either the high or the low frequency.

The maximum voltage-interrupting ability was found at each setting by making tests at increasing voltages until failure occurred. The limit was found to be approximately the same for all of these circuits. The breaker failed on about half of the tests at 3,430 volts 6,750 amperes.

The tests on the plain-break oil circuit breaker operating near its maximum voltage-interrupting ability indicates that conductivity of the arc space following the normal current zero plays a big part in modifying the transients. The very slow high-energy transients are not appreciably affected as the energy loss is relatively low. As the frequency increases, and the energy decreases, the losses through the breaker result in increasing damping of the transients. At high natural frequencies, the admittance of the parallel capacitance path is so much smaller than the conductivity of the arc space that the capacitance current becomes negligible. Consequently, although reducing the capacitance further can greatly increase the natural frequency of the circuit and the calculated voltage-recovery rate, the actual transients obtained on test or in service will not be changed. When the conductivity damps out the higher-frequency components, the lower-frequency greater-energy components become more important. No increase in arcing time occurs with increase in natural frequency above the value where conductivity modifies the transients.

Only at natural frequencies below 2,000 cycles per second, corresponding to less than 45 volts per microsecond, did the arcing time decrease with decrease in frequency. Above 2,000 cycles per second, the arcing time was independent of the natural frequency or of the combination of components in double-frequency transients.

"De-ion Grid" Oil Circuit Breakers

The breaker used for this part of the test had a tank diameter of 30 inches, a stroke of $11^1/_8$ inches, a contact separation of 10 inches and an average opening speed of about 8.2 feet per second. It was equipped with a heavy-duty type of "De-ion grid" of the type shown in Figure 2. The breaker was tested at normal operating voltages but well below its interrupting ability.

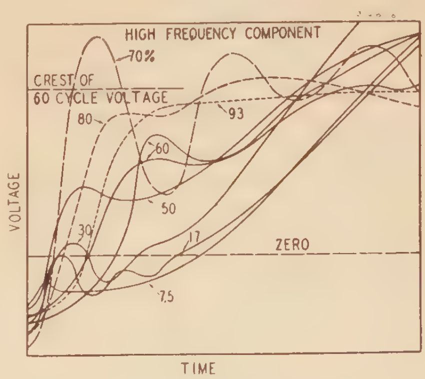


Figure 6. Plain-break circuit breaker, 3,800 volts, 2,600 amperes

Tracings of cathode-ray oscillograms for comparison with calculated transient voltages of Figure 3

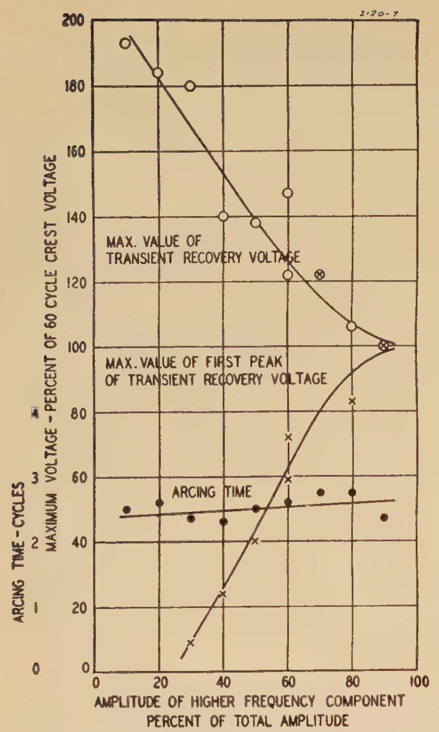


Figure 7. Plain-break circuit breaker, 3,800 volts, 2,600 amperes

Effect of the amplitude of the higher-frequency component

The tests made on this breaker were similar to those made on the plain-break circuit breaker. In many cases the circuits were the same, but the voltages and currents were higher.

One series of tests was made to determine the effect of varying the frequency of a single-frequency transient. This test was made at 7,620 volts 5,350 amperes. Frequencies from 375 cycles per second to 209,000 cycles per second were used.

The arcing time obtained on these tests varied with the natural frequency and voltage-recovery rate as shown in Figure 9. At the lower values the arcing times were less with a gradual rise up to a recovery rate of about 200 volts per microsecond. Above this voltage-recovery rate, no increase in the arcing time was obtained, although values up to 8,500 volts per microsecond were obtained with 209,000 cycles per second.

At the maximum frequency two of the four transient recovery voltages recorded were damped, and two were not damped by the conductivity of the arc space. Throughout these tests, the arc voltage had a tendency to rise and form an extinction peak. This resulted in some increase in the amplitude of the transient. The extinction peak indicates rapid deionization of the arc space and a high effective resistance at current zero. Consequently, damping of the transient was observed only at the highest natural frequency where the total energy of the oscillation was very small.

The preceding tests were supplemented by another series which showed that at 13,200 volts 8,000 amperes, the arcing time was constant for natural frequencies from 3,900 cycles per second up to 200,000 cycles per second. The highest natural frequency was practically completely damped by the energy discharged into the arc spaces, and the transient showed only the 32,000-cycle per second component having an amplitude about 15 per cent of the total. At the next step which had frequencies of 26,000 cycles and 42,000 cycles per second, the highest-frequency component was not appreciably damped, and the extinction peaks were definitely increased.

This breaker was also tested to determine its reaction to double-frequency transients. When the higher-frequency component is either very large or very small, the performance can be expected to be the same as the performance obtained with single-frequency transients corresponding to the predominant frequency. Accordingly, a series of tests was made at 7,620 volts 5,200 amperes with transients having components of two frequencies, one normally causing a shorter arcing time than the other. As the amplitude of the higher-frequency component increased, the arcing time increased as shown in Figure 10. The change in arcing time is gradual, and even small high-frequency components exert a noticeable influence.

These same relations were demonstrated at 15,000 amperes 7,620 volts, as shown on the same curve. The lower frequency varied from 594 cycles per second at the lower amplitude of the high-frequency component to 1,280 cycles per second at the higher amplitudes. These frequencies may be a little higher than those producing minimum arcing and may explain why the arcing time does not increase for high-frequency components greater than about 65 per cent of the total amplitude.

An attempt to get a similar curve at 7,620 volts 2,800 amperes failed, because at the lowest frequency available, 250 cycles per second, the arcing time was as long as at the higher frequencies. The first peak of the transient recovery voltage increased approximately linearly with the increase in amplitude of the higher-frequency component and indicated a negligible damping of the high-frequency component by the conductivity of the arc space. The maximum voltage reached during the transient was highest when one of the two components predominated and was lower when the two components approached each other in magnitude. The arcing time was practically independent of both the first peak and the maximum voltage of the transient.

The tests on this breaker demonstrated that the effect of single-frequency transients varied with the current. At 2,800 amperes no appreciable increase in arcing time resulted from increasing the natural frequency of the circuit above 250 cycles per second. At 5,200 amperes the corresponding point was 3,000 to 4,000 cycles, and at 15,000 amperes it was about 10,000 cycles.

Double-frequency transients demonstrated that the breaker performance varied gradually with the amplitudes of the two components. The limits were the two end conditions which occurred when either the higher-frequency or lower-frequency component was negligible.

Magnetic Blowout Air Circuit Breaker

During the development of a magnetic blowout air circuit breaker, tests were made in the high-power laboratory on single-phase circuits giving about 15,000 amperes at four kilovolts to determine the effect of recovery voltages on the arcing time and voltage-interrupting ability. These values were near the maximum interrupting capacity of the breaker. By

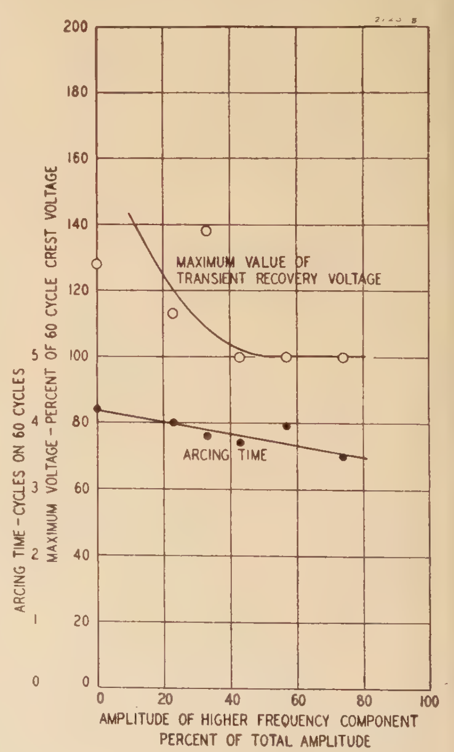


Figure 8. Plain-break circuit breaker, 3,050 volts, 6,000 amperes

Effect of the amplitude of the higher-frequency component

arying the capacitance of the circuit, atural frequencies from 3,880 cycles per econd to 12,000 cycles per second were btained. For still lower transient re- overy voltages a resistor was connected cross the breaker to simulate a parallel esistive load. Some of the transient ecovery voltages obtained are repro- luced in Figure 12.

The arcing time was less than one cycle nd independent of the transient recovery oltage. This indicated that the arc was nterrupted at the first current zero after t had reached a suitable location in the rc chute. The arc voltage prior to in- erruption was relatively high and smooth. The lack of an extinction peak indicated hat there would be some conductivity in he arc space subsequent to the current ero, and this is further indicated by the ubsequent damping of the recovery ransients. The damping is very notice- ble on the circuits having high frequen- ies at which the current through the arc pace is comparable to the current into he capacitance. At the lower frequen- ies the damping is less pronounced, as he current into the capacitance is large vith respect to the current through the rc space.

How large this current into the parallel apacitance can become is illustrated in a triking manner in Figure 13. This is a ection of an oscillogram made on one of he tests at 4.4 kv, a little above the in- errupting ability of the breaker, and on a ircuit having a large parallel capacitance btained by connecting to it three large ower transformers. The natural fre- uency of the transient recovery voltage vas 4,000 cycles per second. Due to the igh test voltage, the arc continued to restrike, and traces of the natural frequency of the transient recovery voltage appear in the arc voltage. The transient at the time of restriking excited the resonant circuit and produced a sinusoidal current in it which modified the current through the circuit breaker sufficiently to produce a sinusoidal ripple on the arc voltage.

With the transient recovery voltages of an oscillatory character, the breaker could interrupt about 4 kv regardless of frequency of the oscillation. By placing a resistor across the breaker to simulate a parallel resistive load, the breaker was able to interrupt 4.95 kv. Possibly the increase in the voltage was due to elimination of the tendency of the transient recovery voltage to reach crest voltages in excess of the normal crest of the 60-cycle recovery voltage.

This type of breaker, having a relatively high arc voltage, appears to be sensitive to the crest value which may be reached by the transient recovery voltage, but the arcing time remained constant from 3,880 to 12,000 cycles per second.

Compressed-Air Circuit Breakers

The compressed-air circuit breaker was the fourth type to be included in this investigation. One series of tests, made on a 15-kv 1,500,000-kva breaker with currents up to 36,000 amperes, showed that for circuit voltage-recovery rates of 1,500 to 2,250 volts per microsecond, the arcing time was substantially constant and lasted only to the first current zero after the contacts had been separating for one-tenth cycle. This resulted from the ability of the breaker to interrupt currents in this range at the first current zero, after contact separation adequate to hold this voltage was reached. Another series of tests over the same range, but with higher recovery rates varying from 2,600 to 3,800 volts per microsecond, showed that the minimum contact separation at which arc extinction could be accomplished varied with current. It corresponded to an arcing time of 0.35 cycle at 6,000 amperes (the same as for the lower recovery rate), and to an arcing time of about one cycle at 36,000 amperes, as shown by Figure 14.

This increase in arcing time with current was not due to a corresponding increase in voltage-recovery rate. The increased arcing time was apparently associated with the increased difficulty of interrupting the heavier currents which have greater quantities of ionized gases and which approach current zero at higher rates.

This is an important relation, since it indicates the nature of the mechanism by which arc extinction is brought about. It indicates that the arc column is being deionized, and that this process requires more time as the diameter of the arc (and its current) increases. If the arc extinction were brought about by a breaking of the arc column and the separation of the two ionized sections of the arc, higher recovery-voltage rate, rather than higher current would require additional arcing time for the introduction of additional arc splitters.

These tests also show the sensitivity of the compressed-air circuit breaker to relatively low high-frequency components in the transient recovery voltage. At the maximum current setting and the lower recovery rate, 2,200 volts per microsecond, the transient was composed of a single component having a frequency of 28,500 cycles per second. For the higher recovery rate, about 2,600 volts per microsecond, the transient had two compo-

igure 9. "De-ion grid" oil circuit breaker, 7,620 volts, 5,350 amperes

ffect of natural frequency of transient recovery voltage

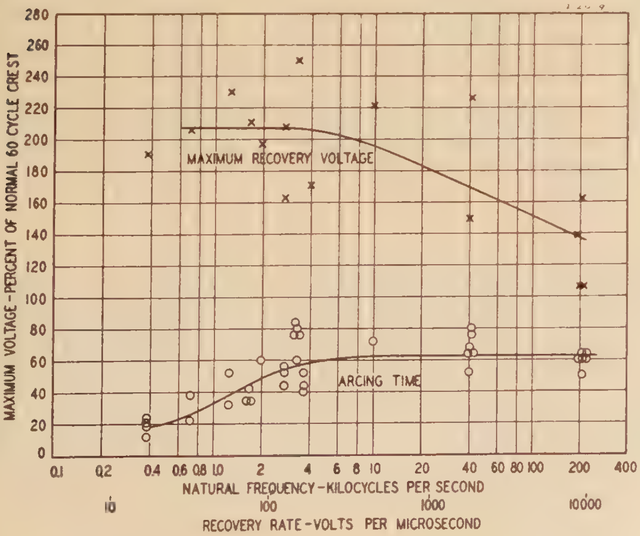
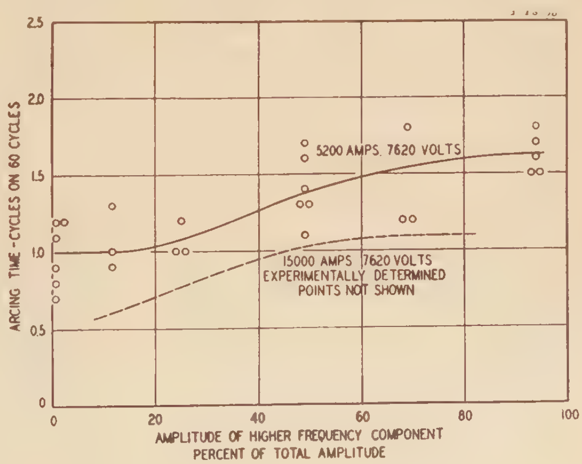


Figure 10. "De-ion grid" oil circuit breaker

Effect of the high-frequency component is roughly proportional to amplitude



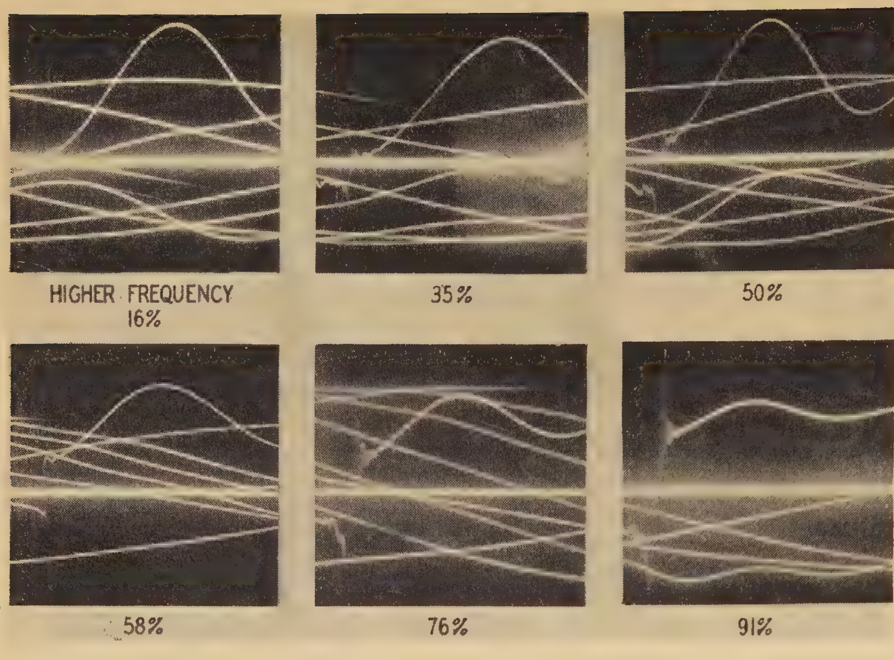


Figure 11. "De-ion grid" oil circuit breaker, 7,620 volts, 15,000 amperes

Cathode-ray oscillograms showing variation in high-frequency component

nents with frequencies of 33,600 and about 190,000 cycles per second. The amplitude of the 190,000-cycle component was only about 25 per cent of the total amplitude of the transient, but as shown by Figure 14, it caused at least a half-cycle increase in arcing time, and the last breakdown occurred at approximately the crest of this first peak of voltage, about 45 per cent of the crest of the 60-cycle recovery voltage.

The circuit transient recovery voltage on this setting, which would be based on zero arc voltage, would have a first crest of voltage about 57 per cent of the 60-cycle crest. This crest is below the 80 per cent which has been used as an arbitrary minimum voltage to be used in calculating circuit transient recovery voltage rates and emphasizes that lower amplitudes must be considered for some types of breakers. The circuit voltage-recovery rate to this first peak is approximately 4,600 volts per microsecond and, in this case, would be a better criterion of the circuit severity than the 2,600 volts per microsecond based on the second peak at 120 per cent of the 60-cycle crest voltage.

Tests made on another breaker of similar design, rated 15 kv 2,500,000 kva, supplemented the tests on the 1,500,000-kva breaker.

Tests varying the natural frequency of the transient recovery voltage from 3,900 to 200,000 cycles per second, and the circuit voltage-recovery rate from 300 to 13,600 volts per microsecond were made at 8,000 amperes 13,200 volts. The arcing time was practically constant over the entire range, varying in minimum and maximum value by only one or two tenths of a cycle.

The series of recovery voltage transients obtained on these tests and shown in Figure 15 is particularly significant and interesting. They show that at the lower frequencies, the transient is almost unaffected by the conductivity of the arc space. With smaller parallel capacitances, the charging current is less, and therefore the current through the breaker is relatively greater. The breaker current at moderate frequencies resulted in partial damping of the transient and finally at high frequencies became the controlling factor when the capacitance current became negligible with respect to it.

Other tests were carried up to 100,000 amperes, and the oscillograms indicated that the larger the current interrupted, the lower the effective resistance of the arc space at the final current zero. The approximate relations are shown on Figure 16. These resistances permitted sufficient current to flow subsequent to the normal current zero to partially damp the recovery transients at the higher currents.

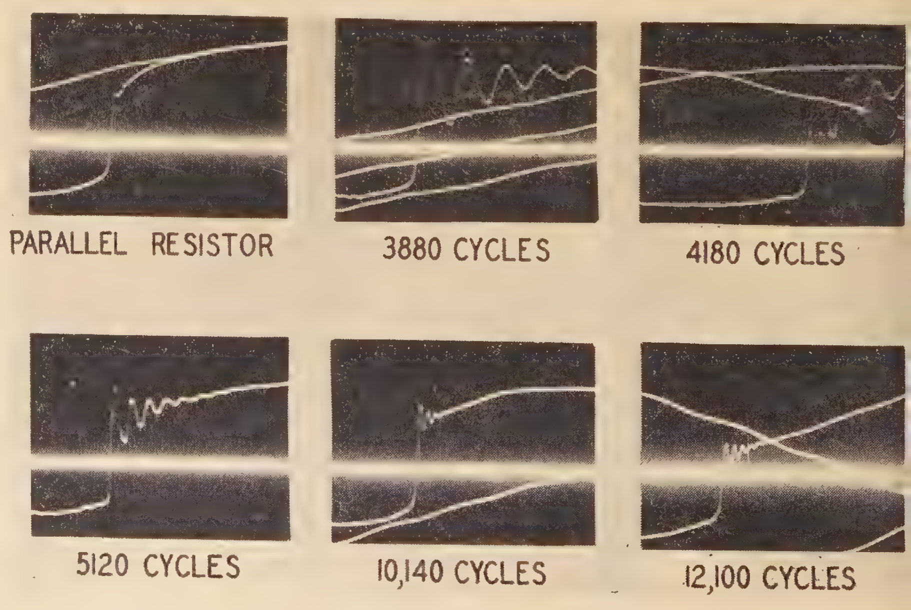
Discussion

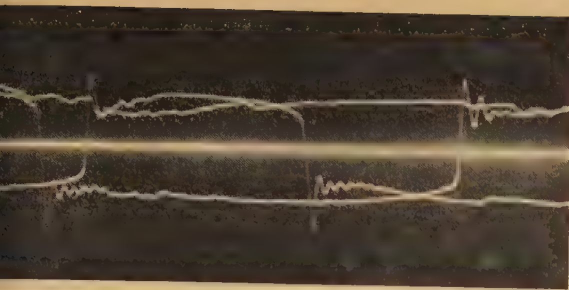
These studies of four types of circuit breakers indicate certain points of similarity in their performance, even though the manner in which arc extinction is produced within the interrupter differs considerably in detail between types. All of these types of breakers modify the transient recovery voltages. The means by which this is accomplished is the same, but the range in which the modification begins to appear, and the range in which it overcomes completely the oscillating tendency of the circuit vary with the types of breakers and not necessarily in relation to their rated voltages and rated interrupting currents.

All of the circuit breakers were tested over a sufficiently wide range to include both modified and unmodified transient recovery voltages. The modification was produced by the breaker when it passed a small current through the arc space subsequent to the final normal current zero. This was evidenced by the transient recovery voltage departing from the values

Figure 12. Magnetic blowout air circuit breaker, 4 kv, 15,000 amperes

Effect of natural frequency of transient recovery voltage





igure 13. **Magnetic blowout air circuit breaker, 4.4 kv, 16,500 amperes**

arge power transformers connected to the ame generator produced these oscillations in e arc voltage during an unsuccessful attempt to interrupt the circuit

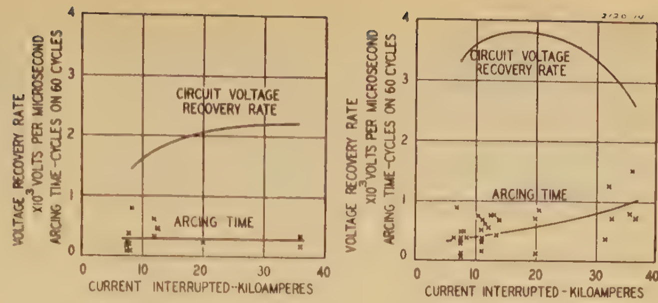


Figure 14. Compressed-air circuit breaker

Arcing time as a function of current interrupted for two series of tests made with different transient recovery voltages

hich could be calculated for the circuit all conductivity of the arc space was eglected. Also, the transient varied om test to test on the same circuit de- ending upon the conditions existing ithin the circuit breaker. The arc onductivity and rate of deionization are dicated roughly by the arc voltage rior to current zero.

The conductivity of the arc space at e time of current zero varies with the urrent being interrupted. This con- uctivity is a function of the ionization uring the preceding half-cycle and the ate at which the space is being deionized. he ionization is produced by the current hich will approach zero at a rate ap- roximately a direct function of the rms urrent being interrupted. The residual nization will depend on how rapidly ese ions are removed.

In circuit breakers which use the energy f the arc to produce the deionizing effect, e rate of deionization might vary di- ectly with the rms current, with the re- ılt that the right amount of deionizing tivity would be present for each current. he larger the rms current being inter- ipted, the greater the rate at which the urrent approaches zero and the stronger e deionizing effect. The production d effectiveness of the deionizing action probably never directly proportional the current. Published data have fre- uently shown that breakers with self- enerated deionizing action have shorter cing times at high currents than at low rrents. These shorter arcing times dicate that the deionizing activity pro- ced by the arc is relatively stronger at e high currents.

Other breakers may have a deionizing tivity produced by some source which is tirely independent of the current to be terrupted. For example, a compressed- r breaker delivers a deionizing blast of r, determined by the design of the circuit breaker and independent of the current being interrupted.

When very low values of current are interrupted, the deionizing activity may keep up with the current, and the conductivity at the current zero may be practically zero. Oscillograms made on various types of breakers show that even a short time before the normal zero in these low-current circuits, the arc may become unstable and extinguish suddenly, the current to the arc space transferring at this time to the path through the parallel capacitance of the circuit and resulting in a rise in the voltage across the breaker at the beginning of the transient recovery voltage. The oscillograms also show that this phenomenon, which mathematically can reach enormous voltages, is still under the control of the breaker, because, as the voltage rises, it cannot exceed the dielectric strength of the space between the contacts without the arc restriking. This dielectric strength, determined by ionization and contact separation, limits the voltages if an attempt is made to interrupt the current so early that the voltage rise is rapid and high. Oscillograms of a large number of tests on several oil and air breakers have indicated that the maximum voltage obtained in this manner probably is not over three times the normal crest of the line to ground voltage.

In other cases, the conductivity of the arc space may become zero as a current zero is reached, and the transient recovery voltage takes place in a manner which can be explained entirely on the basis of the inductance, capacitance, and losses of the power circuit. This type of phenomenon which is generally assumed in calculations may occur only at low currents, or it may extend even up to the rated interrupting current. In general, test data show that as the current is increased, the tendency for some of the ionization to remain at the time of current zero increases. From oscillograms made on the compressed-air circuit breaker, a curve was plotted to show qualitatively the relation between the equivalent resistance of the arc space at the time of current zero and the current which was being interrupted. Other types of breakers, including those having self-generated arc-extinguishing actions, have similar curves.

The residual ionization, lasting for an appreciable time interval after the normal current zero, acts as a variable resistor connected across the terminals of the circuit breaker. This residual ionization or conducting path between the breaker contacts permits a current to flow through the breaker during the time of the transient recovery voltage. This current is a function of the equivalent resistance of the arc space and the voltage impressed across it. It probably has only a very low current density in this space, and the deionizing activity can remove more ions than it produces. Calculations based on the voltage records and knowledge of the circuit characteristics have indicated that currents up to approximately 30 amperes have been carried through the arc space of breakers without the actual formation of an arc. The maximum value of current which can flow during this period without producing an arc probably depends upon the previous current and upon the design of the interrupter.

The actual value of current which flows through the arc space during the transient is, of course, very small with respect to the short-circuit current. In fact, it is too small to be indicated on a normal magnetic oscillogram of the interruption of a short circuit. It flows for only a part of the time consumed by the transient recovery voltage and is detected by its influence on it.

The significance of this small current lies in its magnitude with respect to the charging current which flows into the capacitance in parallel with the breaker. This capacitance can vary within wide limits. Its minimum value corresponds to the capacitance to ground of one terminal of a circuit breaker, a few feet of conductor, and one end of a reactor. Consequently, this current too can be very small. With about 200 micro-

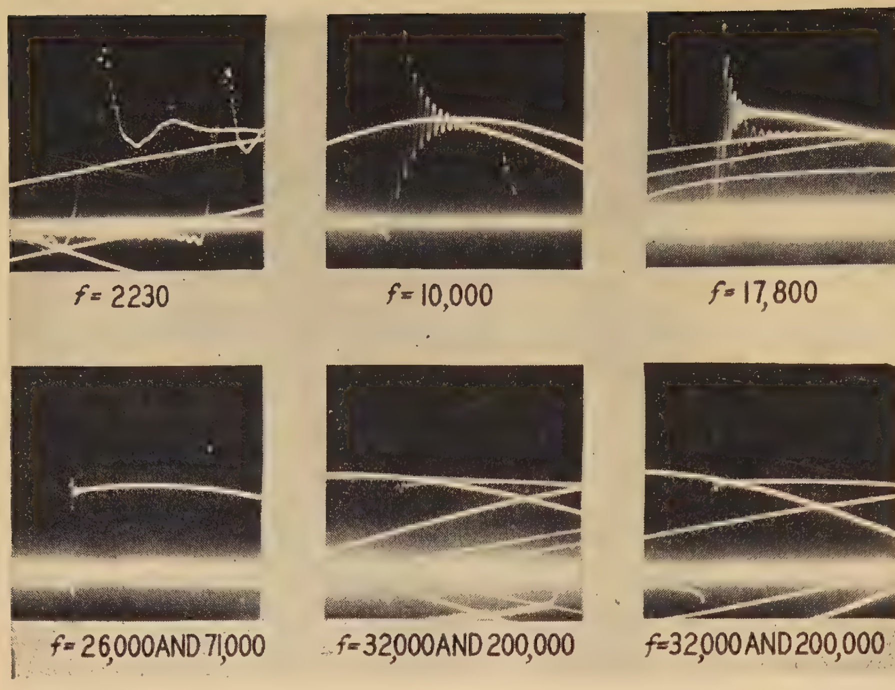


Figure 15. Compressed-air circuit breaker, 13,200 volts, 8,000 amperes

Cathode-ray oscillograms showing the effect of the natural frequency of the transient recovery voltage

microfarads, 200,000 cycles per second, and 7,600 volts rms to ground, it would be about two amperes. When the current through the breaker becomes comparable with the capacitance current, either by the increase of the one or the decrease of the other, it begins to exert a damping influence on the transient recovery voltage and, when large enough, critically damps the transient. For still larger currents through the arc path, the small current flowing into the capacitance becomes negligible, and the transient recovery voltage rises as though it were the voltage appearing across the resistance in a simple circuit containing resistance and inductance in series. As these values of resistance current and capacitive current are relative, the same results can be obtained by reducing the capacitance and thereby the capacitive current until such a point is reached that the current through the arc space is greater than the current through the parallel capacitance. These relations were demonstrated by these tests.

The testing of circuit breakers carried on in high-power laboratories at generator voltage can approximate closely the conditions under which the circuit breaker is to be used in service. The effects, if any, of differences in frequencies of the transients can sometimes be evaluated by a study of the test data to give a definite indication of the manner in which the breaker will perform in service.

The tests on the plain-break circuit showed that at very low values of natural frequency the recovery voltages were undamped. At higher natural frequencies damping of the transient recovery voltage appeared, and after a point of critical damping had been reached, further increase in the natural frequency of the transient recovery voltage resulted in no appreciable change in the actual voltage appearing across the circuit-breaker contacts. This was produced by the effect of the currents through the circuit breaker subsequent to the current zero. This current passing through the breaker was so large with respect to the current flowing into the parallel capacitance of the circuit that it controlled the transient recovery voltage, making it approach an exponential curve. After the point of critical damping was reached, further reduction in the capacitance across the terminals of the circuit breaker did not result in further increase in the severity of the duty on the circuit breaker. This was carried over a wide range above the critical damping without producing any appreciable change in these transients (Figure 5).

A similar set of cathode-ray oscillograms was obtained on the compressed-air circuit breaker, operating at a different current and voltage (Figure 15).

These data demonstrate that if a circuit breaker can successfully interrupt a given voltage and current and critically damp the transient recovery voltage, it can interrupt any other circuit having the same voltage and current and less parallel capacitance without any in crease in the severity of the duty on th breaker.

Conclusions

These tests supplement each other t give a picture of the phenomena of ar interruption by high-power circuit break ers. They show that the interruption brought about by air or oil breakers ar similar in characteristics. In circuit having a low natural frequency, the re covery of the dielectric strength of th arc space proceeds with negligible in fluence exerted upon it by the slowl rising transient recovery voltage. Th maximum value of the dielectric strengtl depends upon the distance between th contacts and the condition of the fluid o fluids filling the space. Above certai values of current and natural frequency depending on the design of the breaker the transient recovery voltage is im pressed on the arc space while consider able ionization still remains. Conse quently, a small current flows througl this space, thereby hindering further de ionization and modifying the transien recovery voltage. For very high natura frequencies this current may exceed th parallel current through the capacitanc across the breaker and may control th transient recovery voltage. If the dis charge current becomes too high, it de velops into an arc which conducts cur rent till the next normal current zero The values of voltage, current, and natu ral frequency at which these phenomen occur vary with the designs of the break ers and not in proportion to their rate interrupting capacities.

The difficulty of circuit interruptio

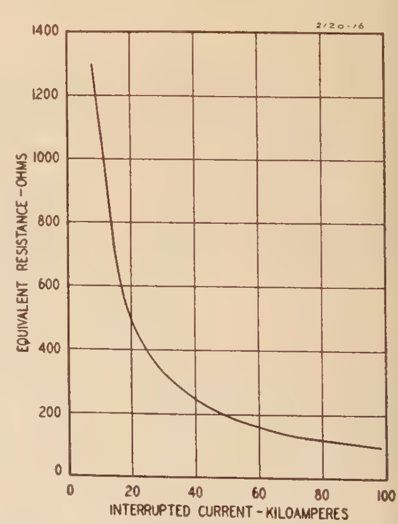


Figure 16. Compressed-air circuit break

Approximate resistance of the arc space the time of the final current zero as a functio of the current interrupted

does not increase indefinitely with increase in the natural frequency of the circuit. The speed with which the circuit tends to apply the transient recovery voltage to the circuit breaker varies directly with the natural frequency. However, if the current passed through the arc space is greater than the current through the capacitance across the breaker, further decrease in the capacitance does not materially increase the actual rate at which the voltage appears across the contacts and, consequently, does not make the circuit more difficult to interrupt.

The maximum arcing times on these tests were reached with transient recovery voltages having natural frequencies which were not heavily damped by the conductivity.

The tests indicate that the effect of the high-frequency component of a two-frequency transient voltage varies almost in proportion to the amplitude of the component. The effect is negligible when both frequencies tend to produce the same arcing time.

The transient recovery voltages obtained on high-voltage circuits energized through transformers can be equalled in high-power laboratories within the range of voltages and currents available. The laboratory circuits closely approximate the severe service conditions as the leads between the transformers and breakers are short. The lead capacitance is small with respect to the transformer capacitance, and, consequently, small variations in it are not significant.

At generator voltages, the reactors in the laboratory circuits have natural frequencies which are sufficiently high to produce the maximum arcing times in circuit breakers.

The plain-break oil circuit breaker at 3,800 volts 2,600 amperes did not increase in arcing time for natural frequencies above about 2,000 cycles per second. Similar points were found for the "De-ion grid" oil breaker, 250 cycles per second for 2,800 amperes, 7,620 volts, 3,000 to 4,000 cycles per second for 5,200 amperes, and 10,000 cycles per second for 15,000 amperes. The compressed-air breaker operating at 8,000 amperes 13,200 volts had the same arcing time for natural frequencies from 3,900 to 200,000 cycles per second, but at higher currents arcing times increased when the natural frequency was over 30,000 cycles per second. The lowest frequency at which the maximum arcing could be reached was not determined, but on a laboratory circuit a natural frequency of about 190,000 cycles was heavily damped, indicating that the maximum arcing time had been obtained.

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Relative Expense for Service Restoration With Different Types of Overcurrent Protection for Distribution Circuits

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THE combination of line sectionalizing in the main feeder, individual protection at branch-line junctions, and reclosing relays and breakers at the substation has been shown to provide a cumulative reduction in consumer minutes outage (minutes of outage per consumer per year) that no one of the methods can provide alone.[1] Branch protection proved to be of greater value than line sectionalizing.

However, in order to be of any real assistance in efficient system planning, such knowledge of the ways and means to improve service continuity must be combined with an understanding of the expense involved in restoring service for all combinations and types of protective equipment that are available. Such "restoration expense" involves the man-hours required to locate the fault, to make any necessary repairs on the line, and to restore service, and involves also the automotive miles which must be traveled in doing it. Naturally, initial costs must be included for an over-all consideration of the economics of overcurrent protection.

Calculations and Presentation of Data

Separate studies were made on two different setups of the distribution lines, namely, one for branch protection, as in Figure 1, and another for line sectionalizing with protective devices connected in series as in Figure 2. The assumptions in the appendix are basically identical with those used in the previous study for the calculation of consumer minutes outage.[1] The minor changes involve interpretations as to what portion of this outage time is for location, repair, and restoration of service, and where automotive mileage is required. Thus, it is possible to correlate directly the service outage value of different combinations and types of protective equipment from the previous study[1] with the values now being presented for the "restoration time" and the automotive miles traveled.

The following special terms are used throughout the paper:

"*Restoration time*" is the minutes per year spent by the trouble crew for all faults from the instant of notification of an interruption in service until all necessary repairs are made and service is restored.

"*Automotive mileage*" is the miles traveled by the trouble crew in performing the duties listed under "restoration time."

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Figure 1. Individual branch-line protection added to 30-mile feeder

Branches located at center of feeder for average of even spacing along feeder

No. Branches	Total Length of Line: 1-Mile Branches	5-Mile Branches	10-Mile Branches
0	30	30	30
1	31	35	40
2	32	40	50
3	33	45	60
4	34	50	70
5	35	55	80
6	36	60	90

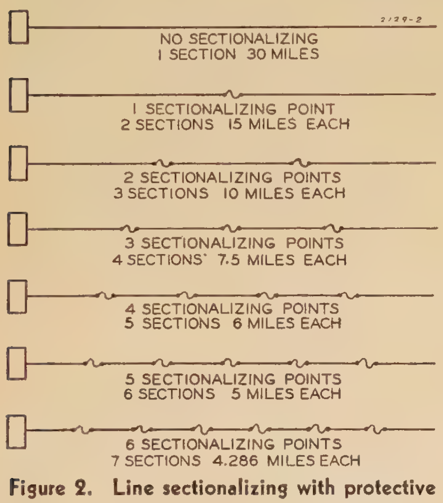


Figure 2. Line sectionalizing with protective devices connected in series on 30-mile line

"*Restoration expense*" is the total expense for "restoration time" and automotive mileage.

The "yardstick" is either the "restoration time" in minutes per year or the automotive mileage per year caused by "permanent faults alone" with no automatic or manual line sectionalizing and no branch protection.

Using the assumptions in appendix A, calculations were made to determine the "restoration time" and the automotive mileage traveled for each system setup, and the values are given as a percentage of the "yardstick" on the curves, Figures 5 to 12 inclusive, that is:

$$\text{Per cent} = 100 \times \frac{\text{Actual calculated values for specific system setup*}}{\text{"the yardstick"*}}$$

By making comparisons in terms of this percentage, we eliminate to some extent the effect of departures of actual practice from the assumptions employed in the study. Wherever such departures might affect the calculations, an attempt was made to be conservative in showing benefits for line sectionalizing or branch protection with the less costly line protective devices.

* The calculated values and the "yardstick" may be in terms of either "restoration time" in minutes per year or automotive mileage traveled per year.

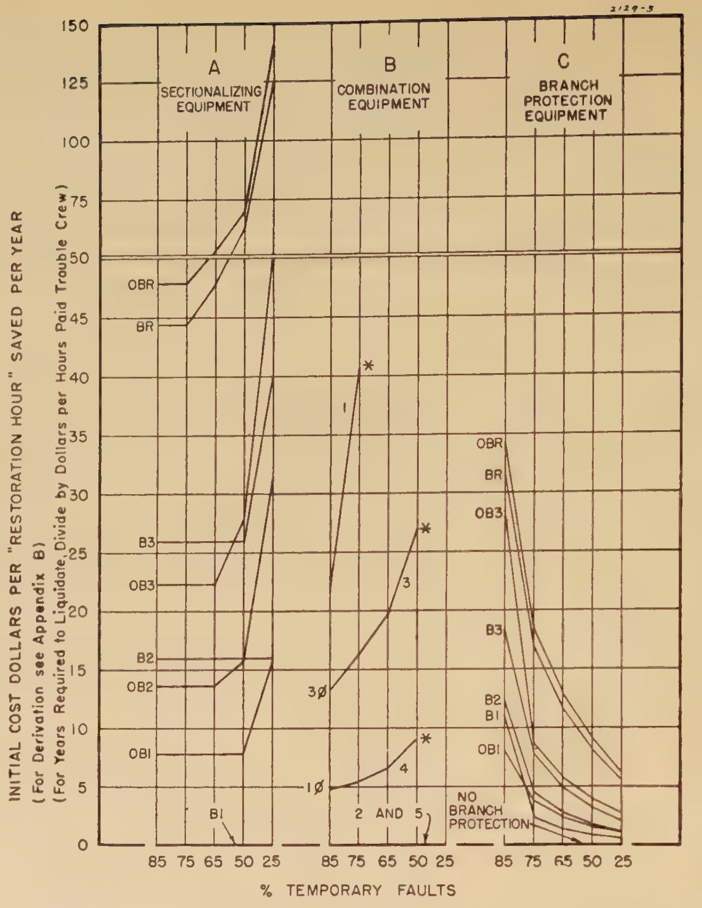


Figure 3. Curves showing cost per hour of "restoration time" saved per year to liquidate relative initial costs of protective equipment

Automotive mileage not included

Symbols defined in Figure 6

Column *A*. Curves for sectionalizing equipment show relative cost of making savings in "restoration time" over that required with single-element fuse cutouts

Column *B*. Curves for different combinations of equipment affording approximately equal service continuity show cost of saving in "restoration time" where made by higher cost equipment. *Such savings not made where curves not extended

Compare curve 1 with 2

Curve 1 for three resetting reclosers at sectionalizing points with three unprotected branches. Curves 2–8 for two-element reclosing cutouts, three at branches, five at sectionalizing points

Compare curves 3 and 4 with 5 (line sectionalizing only)

Curves 3 and 4 for three three-element reclosing cutouts three and one-phase circuits respectively

Curve 5 for five two-element reclosing cutouts one- or three-phase circuits

Column *C*. Curves for branch protection show the relative cost of savings in "restoration time" secured with different types and combinations of equipment

Observe that the cost of all reclosing equipment increases at a higher rate than the decrease in "restoration time" they provide

Known first costs are brought into the picture to determine the length of time required to liquidate any increased initial costs for equipment which provides savings in "restoration expense," as in Figure 3. (See also appendix B.)

The Curves

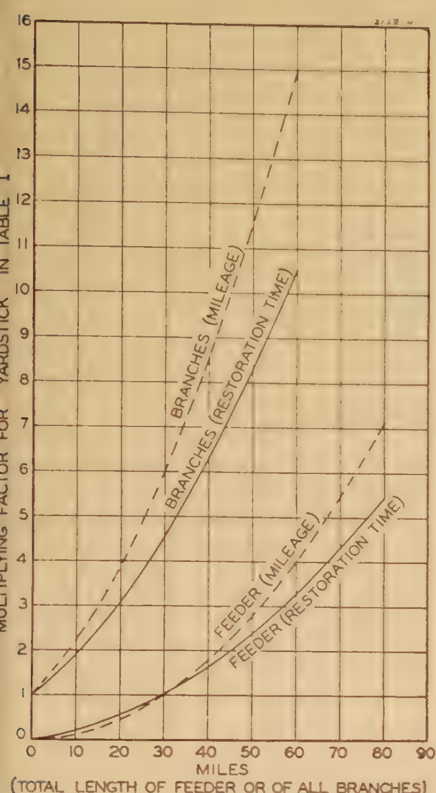
The data are given in curve form showing the percentage of the "yardstick" for varying arrangements of line protection. Figures 5 to 10 inclusive are for *branch protection*, and Figures 11 and 12 are for *line sectionalizing* with a number of protective devices connected in series. While these data may not fit a specific circuit exactly, they should be sufficiently close to be usable in planning the overcurrent protection on practically all distribution systems.

Actual Values for "Yardstick" Affect Comparisons

In addition to comparing the percentages given on the curves, it is important to consider also the value of the "yardstick" and the calculated values for specific cases in terms of the actual "restoration time" in minutes per year or the automotive mileage per year. Since we have assumed a constant number of total faults, the "yardstick," which is based on "permanent faults alone," is obviously lowest with 85 per cent temporary faults. It increases to a maximum at the 25 per cent temporary faults as shown in Table I. These values would increase or decrease respectively if extended beyond the 25 to 85 per cent range that was studied.

Much greater "restoration time," automotive mileage, and consumer minutes outage are required to locate, repair, and restore service with a permanent fault than just to restore service with a tem-

porary fault. Thus, as the percentage of emporary faults becomes greater, the 'restoration time," automotive mileage, and consumer minutes outage caused by equipment opening on temporary faults ncreases less than the decrease in these values as caused by permanent faults. Because of this relationship, the calculated actual "restoration time" and auto-



igure 4. Conversion curves for determining 'yardstick" with different lengths of feeder and total length of branches

How to use data:

1. Determine the multiplying factor

1). For the proper length of the feeder

2). For the total length of all the branches

2. Multiply the "yardstick" in Table I by the multiplying factor for the feeder

3. Multiply the corrected "yardstick" determined in item 2 by the multiplying factor for the branches

Observe that the "yardstick" is increased more by adding mileage to branches than by increasing the length of the feeder

motive mileage for 25 per cent temporary faults are greater than that for 85 per cent temporary faults. This relationship is reversed when the calculated values are given as a percentage of the "yardstick" as on the curves, Figures 5 to 12 inclusive. This shift in relationship is important when evaluating the benefits secured by the use of the more effective device which usually costs more (see Figure 3).

Some Protective Devices Can Be Connected in Series in Greater Numbers Than Others

More two-element than three-element reclosing fuse cutouts can be connected in series and co-ordinated to provide about equal improvement in service continuity. Figures 11 and 12 indicate that the use of a greater number of two-element than three-element cutouts may increase the normal differences in "restoration expense" for these two devices at the higher percentages of temporary faults. However, when the difference in the initial costs of these two types of cutout is divided by the savings in "restoration expense," it shows that the cost per hour of "restoration time" saved per year by the use of the three-element design would be quite high, especially for three-phase circuits. For example, the use of two-element cutouts at five sectionalizing points and three-element cutouts at three sectionalizing points provides approximately the same service continuity. A comparison of these, as in Figure 3 (column *B*, curves 3, 4, and 5) indicates that from 6 to 13 years might be required to liquidate the extra cost of three three-element cutouts per phase on a three-phase 30-mile circuit, with a wage rate of one dollar per hour per man for a two-man crew.

A similar comparison is made in Figure 3 (column *B*, curves 1 and 2) to show the effect of the limitations in co-ordinating automatic resetting reclosers with fuses at branch junctions or at transformers. The use of three two-element cutouts to protect three branches added to a line sectionalized with the five two-element cutouts provides at least equal service continuity to that provided by three reclosers at sectionalizing points on the same line with the three branches unprotected. With 85 and 75 per cent temporary faults Figure 3 indicates that from 11 to 20 years respectively will be required to liquidate the additional cost of the reclosers with a wage rate of one dollar per hour per man for a two-man crew. With 65 and lower percentages of temporary faults the use of the two-element reclosing cutouts in the branches makes possible lower "restoration time" as well as a lower initial cost and a slight improvement in service continuity.

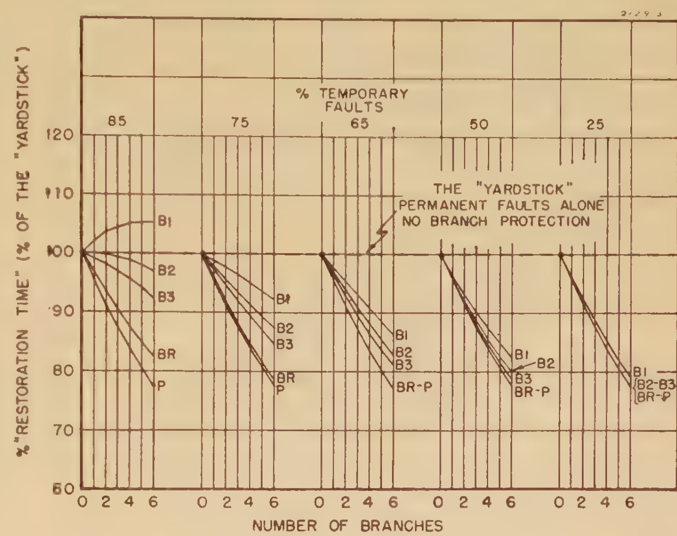


Figure 5. "Restoration time" for protected one mile branches employing nonreclosing or reclosing substation breakers and all available types of branch protective equipment

Symbols defined in Figure 6

Observe that a saving is made for all but single-element fuse cutouts when used with nonreclosing substation breakers at 85 per cent temporary faults

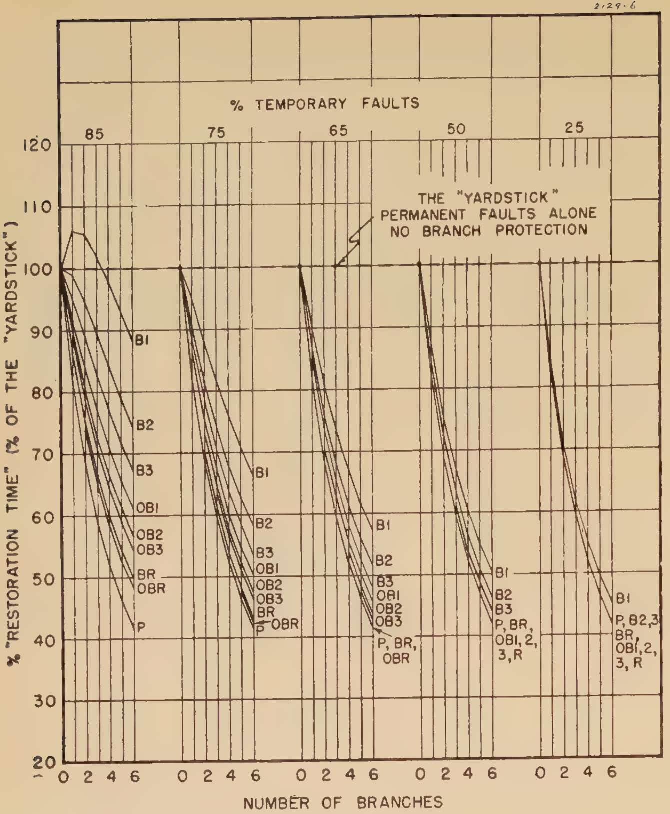
Effect of Nonreclosing and Reclosing Substation Equipment

With no line sectionalizing or branch protection, "permanent faults alone" are the only cause for sending out the trouble crew. The restoration of service after a temporary fault by manual or automatic reclosing of the substation breaker involves only the station attendant and thus the type of equipment employed has no effect on the "restoration time" or automotive mileage.

Combining Branch Protection and Line Sectionalizing

The cumulative effect on "restoration time" and automotive mileage of combining the protection of a number of short branches with line sectionalizing can be approximated from the data on the curves, Figures 5 to 12 inclusive. See appendix C for description of how to do this.

Where the circuit consists of two or more long branches, each branch should be studied separately from the standpoint of line sectionalizing and branch protection. Then the cumulative effect should be determined by adding or subtracting



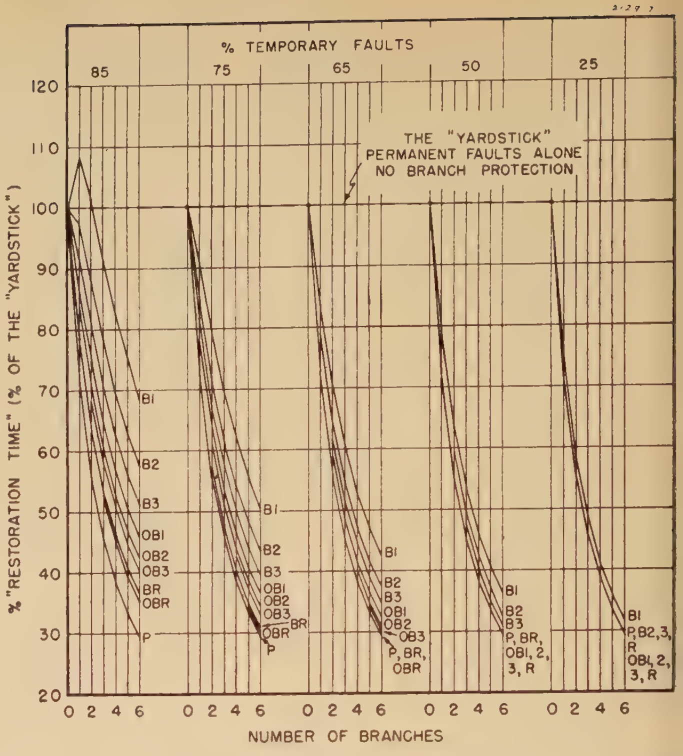


Figure 6 (left). "Restoration time" for protected five-mile branches employing all available types of substation and branch protective equipment

B=Nonreclosing or reclosing breaker at substation

OB=Overlapping reclosing breaker at substation, set for instantaneous tripping on first opening with all types of line protective equipment delayed to open only ahead of second opening of breaker

1=Single-element fuse

2=Two-element reclosing fuse

3=Three-element reclosing fuse

R=Automatic resetting recloser

P=Permanent faults alone—all temporary faults cleared with only a momentary outage

(These are combined to show type of substation and line equipment employed)

Observe:

1. The major reduction in "restoration time" for "permanent faults alone" with the branches protected

2. The comparatively small increase in "restoration time" for outages caused by equipment opening on temporary faults

3. Single-element fuses provide the major portion of the reduction obtainable except at 85 per cent temporary faults

Figure 7. "Restoration time" for protected ten-mile branches employing all available types of substation and branch protective equipments

Symbols defined in Figure 6

Observe that the reduction in "restoration time" is greater with ten- than with five-mile branches with all other relationships being proportionally the same

the percentages as described in appendix C.

For Reclosers Which Do Not Reset Automatically

The conclusions drawn and the data in the curves for reclosing fuse cutouts apply to any type of apparatus with an equal number of reclosers and without the feature of automatically resetting after clearing a temporary fault.

Conclusions

Some general conclusions can be drawn from the study, although it is anticipated that the data will prove even more helpful in system planning when applied to the problems of specific circuits.

Substation Equipment

1. The substitution of reclosing for nonreclosing breakers at the substation, while improving service continuity, has no effect on the "restoration time" or automotive mileage (unless the substation attendant's time to manually reclose the breaker is classed as "restoration time").

2. Overlapping the substation reclosing protection with all line protective devices provides a major reduction in "restoration time" and automotive mileage. It is quite effective when combined with branch protective devices. (With such overlapping the breaker trips and recloses once without the branch or line protective device opening for all faults out to the ends of the line, and then the relay provides time delay so that the branch or line-sectionalizing protective device disconnects the faulted portion of the circuit ahead of the second tripping of the relay.)

3. Such overlapping protection of the entire line is the most economical of all system setups providing automatic reclosing, when both "restoration expense" and initial costs are considered.

4. Where such overlapping protection as in paragraph 2 reaches only part way out on the line, the decrease in "restoration time" and automotive mileage over a nonreclosing or reclosing breaker is only approximately one quarter of the decrease provided by overlapping the whole line.

Branch Protection Reduces Maintenance

1. Overcurrent protection of individual branches generally reduces "restoration time" and automotive mileage, even with short branches. The exceptions are with only a few or very short branches at high percentages of temporary faults where the actual number of minutes and miles are small (see Figures 5 to 10 inclusive).

2. The length of the branch has a major effect on the "restoration time" and automotive mileage.

3. Overcurrent protection of individual branches provides less reduction in terms of actual "restoration time" and automotive mileage at higher percentages of temporary faults than at the lower percentages.

4. The savings in "restoration expense" with branch protection are less likely to liquidate the initial cost of all types of overcurrent protective equipment at higher percentages of temporary faults than at

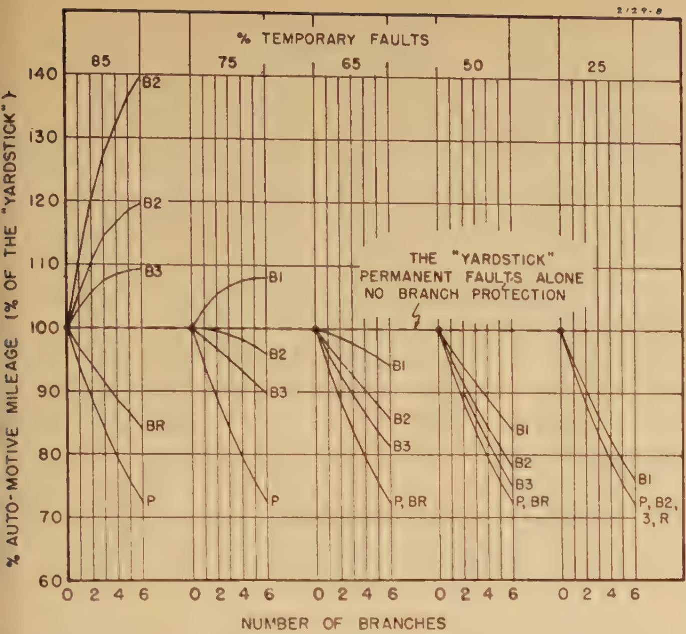


Figure 8. Automotive mileage for protection of one-mile branches employing nonreclosing or reclosing substation breaker and all available types of branch protective equipment

Symbols defined in Figure 6

Observe that the saving in automotive mileage for "permanent faults alone" is greater than the saving in "restoration time" (Figure 5). Also that the effect of outages caused by temporary faults is greater. However, the actual values for the "yardstick" in terms of automotive mileage is much less than for "restoration time"

the lower percentages, where the protective orbs of the substation relay and breaker includes the whole line.

5. Single-element fusing of branches, five miles and longer, is the most effective in saving sufficient "restoration time" and automotive mileage to liquidate the initial investment. Only a few years are required because of the comparative low first cost to the savings effected. For example, with 30 miles of single-phase branches on a 30-mile feeder and a wage rate of one dollar per hour per man for a two-man trouble crew, the liquidation would be effected in about 1 to 2.5 years. The exception is with 85 per cent temporary faults where the actual minutes and miles saved are too small to offset the initial cost of even single-element cutouts within a reasonable time.

6. Two and three-element reclosing fuse cutouts and automatic resetting reclosers provide reductions in "restoration time" and automotive mileage which are worthy of consideration. However, this saving is not proportionate with the increase in initial costs over that for single-element fuse cutouts.

7. Reclosing breakers at the substation which overlap single-element fuses at a num-

Figure 9 (below). Automotive mileage for protected five-mile branches employing all available types of substation and branch protective equipment

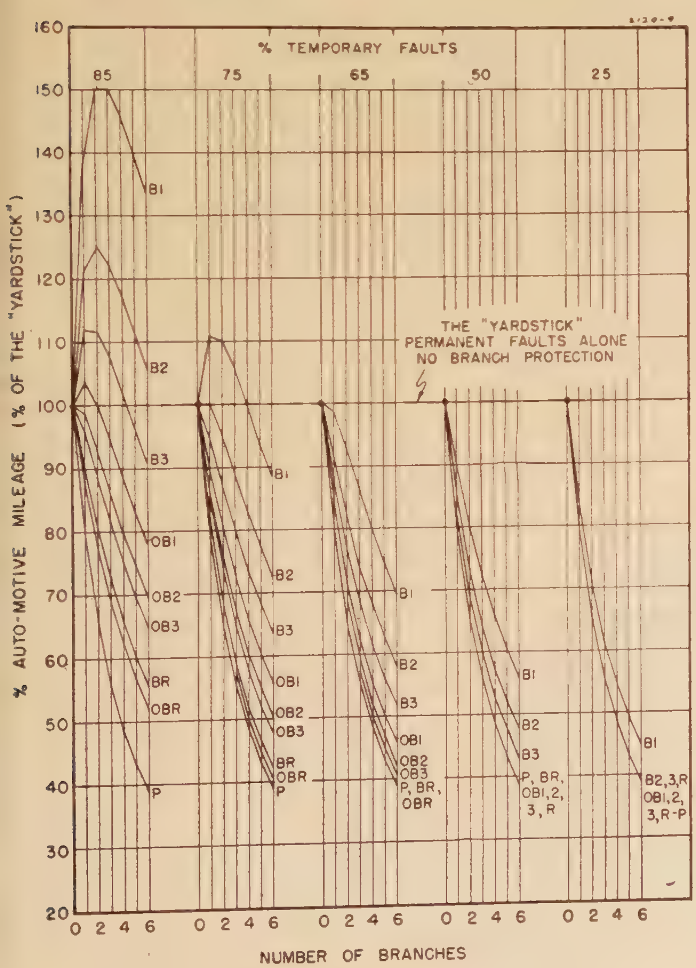
Symbols defined in Figure 6

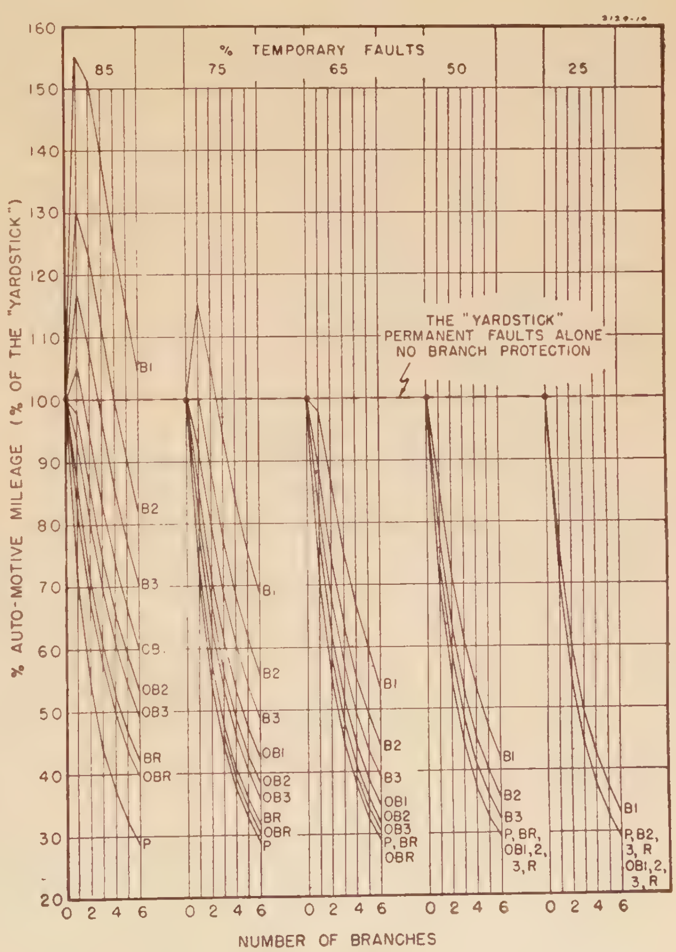
Observe the same greater reduction in automotive mileage for "permanent faults alone" and the effect of outages caused by temporary faults as observed in Figure 8

Figure 10. Automotive mileage for protected ten-mile branches employing all available types of substation and branch protective equipment

Symbols defined in Figure 6

Observe that the reduction in automotive mileage is greater than with one- and five-mile branches (Figures 8 and 9), and that the effect of outages due to temporary faults is proportionately greater, especially at the higher percentages of temporary faults





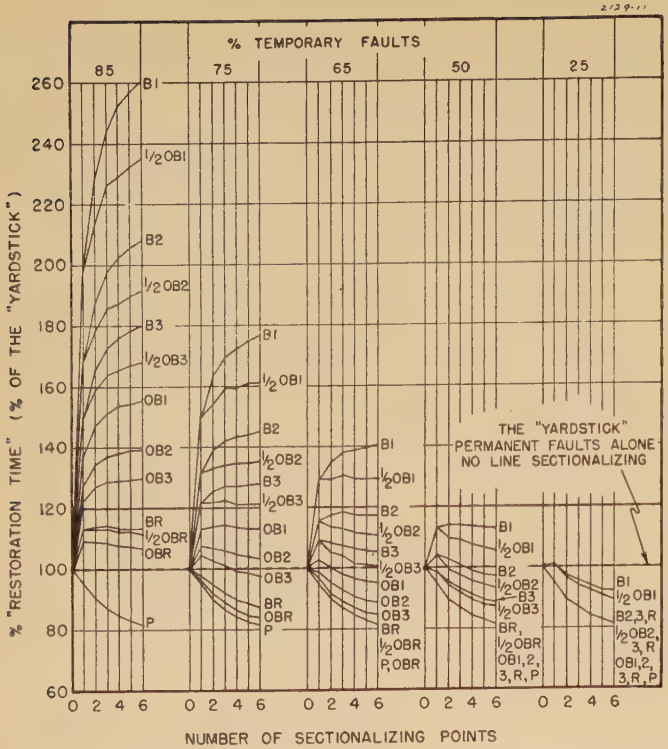


Figure 11. "Restoration time" with line sectionalizing employing all available types of substation and line overcurrent protective equipment

$^1/_2$*OB* = overlapping reclosing breaker at substation, protection for only half of line

Other symbols defined in Figure 6

Observe:

1. The "restoration time" as caused by "permanent faults alone" is reduced only slightly by line sectionalizing
2. The effect of outages caused by devices opening on temporary faults is greater than with branch sectionalizing
3. Therefore, line sectionalizing generally increases "restoration time"

(This increase amounts to only about ten hours per year with single-element fuses at 85 per cent temporary faults on a 30-mile feeder)

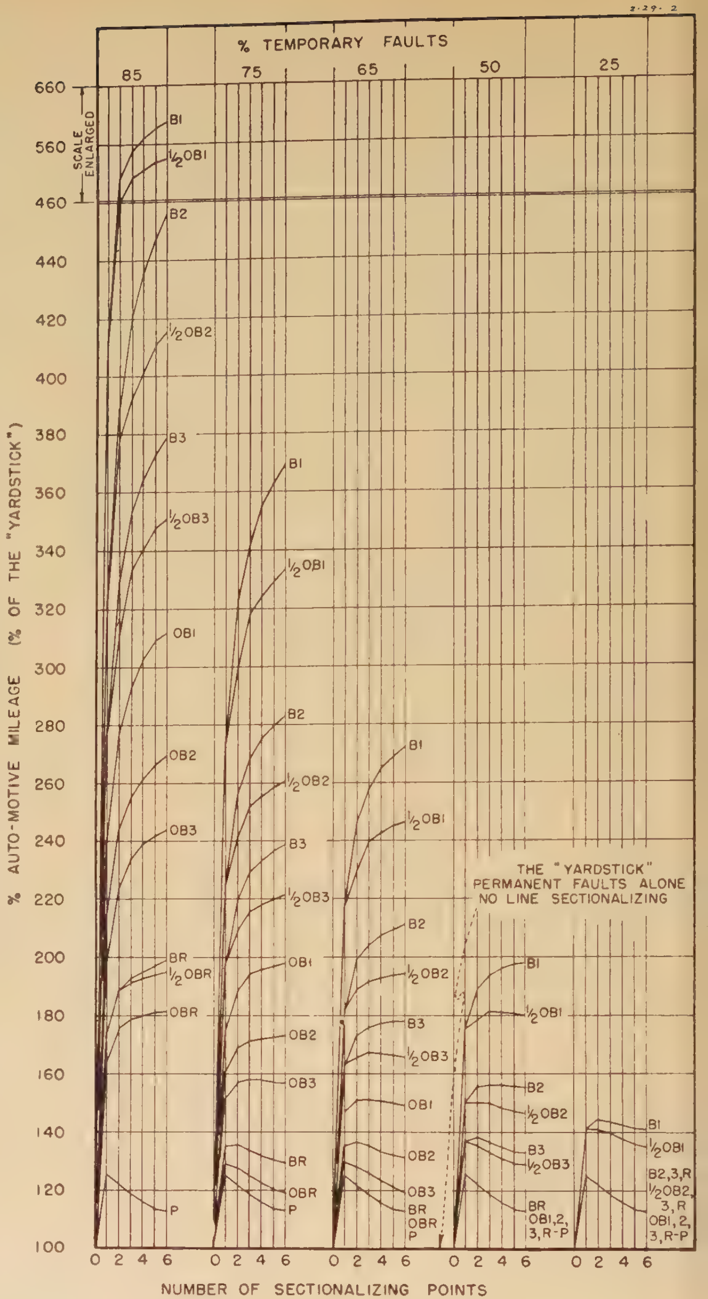


Figure 12. Automotive mileage with line sectionalizing employing all available types of substation and line overcurrent protective equipment

$^1/_2$OB = overlapping reclosing breaker at substation, protection for only half of line

Other symbols defined in Figure 6

Observe:

1. The automotive mileage even for "permanent faults alone" is increased by line sectionalizing
2. As with branch protection, the effect of outages caused by temporary faults is greater than with "restoration time" (Figure 8)

ber of branch junctions provide more savings in "restoration time" and automotive mileage than three-element cutouts with reclosing breakers. This combination is almost as effective as single-element fusing of branches without overlapping protection in having the savings in "restoration time" offset the initial costs, at 75 and lower percentages of temporary faults, and is more effective at 85 per cent.

8. Consideration of the economic value of reducing the percentage of temporary faults appears to be justified. Such a reduction for the line as a whole might be obtained by the use of reclosing devices on a branch or branches having a much higher percentage of temporary faults than the line as a whole.

Automatic Line Sectionalizing

1. Automatic sectionalizing of the main feeder (and long branches treated as feeders) with protective devices connected in series generally increases "restoration time" and automotive mileage and thus must be justified by the savings in revenue and the improvement in customer good will resulting from the better service continuity. However, the automatic resetting reclosers reduce "restoration time" and automotive mileage, as do two and three-element reclosing fuse cutouts at 50 per cent temporary faults and lower, and reclosing breakers which overlap single-element and reclosing cutouts at 65 per cent and lower (see Figures 11 and 12).

2. The increase in actual "restoration time" and automotive mileage even with single-element fuse cutouts generally is comparatively small.

3. Automatic sectionalizing with single-element cutouts requires the most additional "restoration time" and automotive mileage but at a minimum over-all cost, see Figure 3, column 1. Therefore, sectionalizing with single-element cutouts may be justified economically in many instances.

4. Overlapping reclosing substation breakers combined with single-element fusing at sectionalizing points requires less additional "restoration time" and automotive mileage than sectionalizing with three-element reclosing cutouts and with a lower total first cost. This is especially true on three-phase circuits. (Only the additional equipment for the special relaying was considered necessary at the substation.)

5. Sectionalizing with automatic reclosing equipment provides a greater reduction in "restoration time" and automotive mileage compared to single-element cutouts than when the comparison is made with branch protection. However, as in the case with branch protection, the reduction in "restora-

ion time" and automotive mileage is not proportional with the increase in initial cost except possibly on lines appreciably onger than 30 miles.

BRANCH PROTECTION AND LINE SECTIONALIZING SHOULD BE COMBINED

The cumulative benefit obtained by combining branch protection and line sectionalizing should not be overlooked. Sectionalizing with equipment, the characteristics of which are such as to preclude branch protection, prevents the securing of major reductions in "restoration time" and automotive mileage.

MANUAL LINE SECTIONALIZING

Manual line sectionalizing generally is employed where automatic protection has not been adopted. With manual sectionalizing "restoration time," automotive mileage, and consumer minutes outage are caused by "permanent faults alone" and thus are the same percentages of the "yardstick" at all percentages of temporary faults. Therefore, automatic sectionalizing and branch protection, which may cause outages on all or some temporary faults, are less likely to be justified over manual sectionalizing at the higher percentage of temporary faults (above 75 per cent) than is indicated when compared with the "yardstick." This is especially true on a feeder with relatively long branches where manual sectionalizing eliminates traveling out and back on a number of the branches in search of the fault.

LOWERING OF THE PERCENTAGE OF TEMPORARY FAULTS SHOULD BE CONSIDERED

. Since outages caused by temporary faults make all types of equipment less efficient in attaining the minimum values for "permanent faults alone," consideration should be given to the economics of reducing the percentage of temporary faults, especially in the range above 75 per cent.

2. If the percentage of temporary faults is decreased, less costly equipment generally will provide equal or better service continuity with lower "restoration time" and automotive mileage.

3. A reduction in the percentage and thus the number of temporary faults on a specific system lowers the total number of faults and provides greater reductions in "restoration time" and automotive mileage than is shown in this study, where the total number of faults remain constant.

Use of Data for Continuing Expansion of Knowledge

Although over-all values of combining branch protection, line sectionalizing, and reclosing relaying at the substation as presented are based on a set of rigid assumptions, these data should facilitate more efficient planning of overcurrent protection on specific systems. It is hoped that these data will be extended, or if necessary, modified as the result of operating records secured in actual practice.

Table I. Actual Calculated Values⊖ for "Yardsticks" for Feeder Without any Branches† or Manual Line Sectionalizing

Values Decrease as the Percentage of Temporary Faults Increases

Units in Which Calculated Actual Values Are Expressed	Per Cent Temporary Faults 25	50	65	75	85
	Actual Values for Permanent Faults Alone*				
Consumer minutes outage per year	2,137.5	1,425	996.25	712.5	427.5
"Restoration time" in minutes per year	2,137.5	1,425	996.25	712.5	427.5
Automotive mileage per year	337.5	225	157.5	112.5	67.5

⊖ The values in this tabulation are based on a 30-mile feeder. To convert the values for longer or shorter feeders, use the multiplying factors as determined in Figure 4.

* These actual values will not apply exactly to any system, but the relationship shown should apply about proportionally. To determine actual "restoration time" and automotive mileage for any system setup, multiply the "yardstick" by the percentage for that setup as given on curves 5 to 12 inclusive.†

† The values in this tabulation do not apply directly to feeders with branches. Corrections can be made by use of the multiplying factors for the total length of all branches as determined from Figure 4.

Appendix A. Assumptions Employed in Mathematical Study

The same general assumptions employed in the study to determine the relative value of different types of overcurrent protection for distribution circuits in terms of service continuity[1] were used in continuing the mathematical study to check the effect on "restoration expense." All of the assumptions pertinent to the present study are tabulated below:

1. *The Distribution Lines Studied (Length of Lines)*

(*a*). In one part of the study one to six unprotected and protected branches, each one, five, or ten miles in length, were added at the mid-point of an unsectionalized 30-mile main feeder, increasing the total length of line, as in Figure 1. (The greater total length will increase the actual "restoration time" and automotive miles, but when these are expressed as a percentage of the "yardstick" (for "permanent faults alone" without branch protection), the effect of the length is cancelled. Thus the percentage shown on curves of Figures 5 to 10 inclusive for any system setup can be applied to any length of line or branches, if the "yardstick" is modified as directed in Figure 4. Connecting the branches at the mid-point provided an average for an even spacing of the branches along the feeder.)

(*b*). In the other part of the study, a 30-mile main feeder was broken up into from one to seven sections by zero to six overcurrent protective sectionalizing devices connected in series as in Figure 2.

2. *Equipment Employed.* Both studies included checking comparative results with overcurrent protection provided by:

(*a*). Substation breakers actuated by nonreclosing or automatic reclosing relays, and automatic reclosing relays which overlap all branch and line protective equipment so that the breaker trips and recloses once without the branch or line devices opening, and then provides time delay before the second tripping of the relay on more persistent faults; each combined with

(*b*). Sectionalizing or branch-line protection with single-element or two and three-element reclosing fuse cutouts or reclosers which reset automatically after clearing a temporary fault.

3. *Number and Type of Faults.* One fault per mile per year uniformly distributed with temporary faults equaling 25, 50, 65, 75, and 85 per cent of the total. (One fault per mile is probably somewhat high. Any lower value might have been used without changing the percentages used for comparison but would decrease the values for the "yardstick" in Table I and would change the values in Figure 3—decreasing these values for cost of "restoration" with line sectionalizing in column *A* and increasing the values for the cost to secure the savings with branch protection in column *C*).

4. *Attended Substation.* No "restoration time" was included for the substation attendant to close the station breaker or to notify the trouble crew. ("Restoration time" for temporary faults was zero, regardless of whether service was restored by manual or automatic reclosing of the station breaker. Thus, there is no difference in man-hours of "restoration time" or automotive mileage for nonreclosing or reclosing relays and breakers.)

5. *Trouble Crew.* Consisted of one man who was always available at the substation to start instantly with no time allowed for notification or preparation. (More often the crew consists of two men to meet safety requirements, but any multiple of one will have no effect on the percentages used in the presentation of the data. The actual "restoration time" in Table I can be multiplied by the exact number in the crew if over one, to make the values comparable with the utility's actual practice, see also the directions on Figure 3. Also, the crew is more likely to be out on the system, sometimes closer to and sometimes farther from the fault location, so that this assumption provided an average.)

6. *Time to Locate Fault, Make Necessary Repairs, and Restore Service.* The trouble crew:

A. With no branch protection or line sectionalizing (All temporary faults cleared when breaker locks open. Service restored by station attendant. Trouble crew required for "permanent faults only.")

(*a*). Traveled 15 miles per hour to the mid-point of the feeder as the average, and out to the mid-point

of each branch and back as the average for each branch.

B. With automatic line sectionalizing and individual branch protection

(*a*). Traveled 30 miles per hour to the sectionalizing point at which the protective device had opened. No time was allowed for examining sectionalizing or branch protective devices en route, as it was assumed that these would have indicating features visible from the car.

(*b*). Spent five minutes to climb the pole and to restore service if a temporary fault had caused the outage at the sectionalizing or branch protection point.

(*c*). Traveled 15 miles per hour to the mid-point of the branch or section on which the fault persisted. The mid-point provided the average for the uniform spacing of faults.

(*d*). Spent 30 minutes repairing a permanent fault. In a majority of cases the trouble crew will repair faults without calling for the assistance of the line crew, generally in less than 30 minutes. Where the services of the larger line crew are required, the time would generally exceed the 30 minutes average. This makes the values presented more conservative than actual practice.

(*e*). After repairing the fault:

1. No time was allowed to notify the substation attendant when he was required to close the breaker (although some time probably would be required for notification which would have increased the values of the "yardstick" Table I and the "restoration time" or automotive mileage for outages caused by temporary faults. This would have shown greater benefits for line sectionalizing and branch protection for reclosing cutouts and especially for automatic resetting reclosures).

2. The crew traveled 30 miles per hour to return to the sectionalizing or branch protective device to restore service.

3. No "restoration time" or automotive mileage was included after service was restored, since the crew might then be headed for another job to which the additional mileage and time would be charged.

7. *Service Restoration by First, Second, Third, and Fourth Reclosures* was assumed to be 50 per cent of the total number of faults for devices which reclose once, 65 per cent for those which reclose twice, 70 per cent for those which reclose three times, and 73 per cent for combinations which reclose four times (see Table IV in appendix of previous study[1]). This gives maximum advantage to three-element reclosing fuses and other multireclosing devices, as compared with two-element reclosing fuse cutouts, since these values for perfect operation are low for the first reclosing and high for the second as compared with operating experience. Some data show as high as 75 to 90 per cent restoration of service following the first reclosure.

8. *No Inspection With Reclosing Fuse Cutouts Was Assumed.* Because such inspection would have permitted refusing before an outage occurred, the data indicate only the minimum benefit for these devices.

(This benefit would be increased to a degree approaching that of the device that resets automatically, as such fuse renewal before the occurrence of an outage approaches 100 per cent. The percentage of such discovery and renewal is fairly high, because linemen and trouble crews are on the lookout for the indicating devices as they pursue their regular duties along the lines.)

Fuse renewal is accomplished in a few minutes while they are at the installation, without any traveling time or mileage as was included in the calculated values presented. Thus all reclosing fuse data are ultraconservative.

9. *The Protective Orbit of the Substation Breaker* (which is determined by minimum pickup current of the relay) included the whole feeder and all of the branches. In many instances distribution lines have outgrown this orbit. Consequently, the lower current individual protection of the branches and line sectionalizing on the otherwise unprotected portion may prevent burning down lines or annealing of the conductors. Such faults would require a long time to repair and might necessitate the assistance of the line crew. Conversely, this lower current protection may cause outages which might otherwise have burned clear. Depending on the type of equipment, after such outages service will be restored automatically or will require the trouble crew to go out to the sectionalizing or branch protection point in order to restore service. There would have to be a large number of such trips to equal the "restoration time" required for one case of putting back up lines burned down because of lack of protection.

Table II

Assumed Initial Costs Used for Figure 3	Cost Each†
Single-element fuse cutouts	$ 7.00
Two-element fuse cutouts	17.00
Three-element fuse cutouts	32.00
Automatic resetting reclosers	85.00
Additional relay equipment*	58.00

† These initial costs are approximately correct. Additional material other than the crossarm will be negligible as mounting hardware is furnished with the devices. Installation costs are not included as they will vary on different systems.

* It was assumed that induction-type reclosing relays and breakers are available.

No actual data are available on this relationship. However, it would appear that the "restoration time" in the area beyond the protective orbit of the station breaker would be reduced perceptibly by line sectionalizing and branch protection. This would decrease the higher "restoration time" and automotive mileage shown in this study for line sectionalizing, where the protective orbit encompassed the whole line, and would increase the savings shown for branch protection.

Appendix B. How to Determine the Cost Per Hour of "Restoration Time" Saved Per Year to Liquidate Relative Initial Costs of Protective Equipment

This is the method employed in figuring the values in Figure 3. It is applicable to the studying of automotive mileage costs as well (substitute mileage values for "restoration time" and proceed as described). Since with line sectionalizing the "restoration time" is increased over that for no sectionalizing, the comparison is made with the single-element cutout which requires the greatest "restoration time." Branch protection generally provides a reduction in "restoration time" from that with no protection and thus the comparison is mad with the "yardstick" to determine the co of securing this reduction.

Procedure

1. Determine the percentage for specif system setup from curves 5 to 12 inclusiv making additions for the cumulative effec of combining branch protection and lin sectionalizing as described in the text.

2. Determine the actual "restoration time for each specific system setup by multiplyin the "yardstick" from Table I (modified necessary for variations in length of th total line as described in Figure 4) by th percentage determined in paragraph 1.

3. Determine the difference in actual "res toration time" for the equipment being com pared.

4. Determine the difference in initial cost of the equipment required for each syste setup (see Table II).

5. Divide the difference in costs by the di ference in "restoration time" to determin the cost per hour of "restoration time" pe year.

6. Divide the values determined in para graph 5 by the total wages per hour paid th trouble crew to determine the number years required to liquidate the difference i initial cost (as determined in paragraph 4 Formulating this:

$$\text{Cost per unit}\dagger\text{ saved/year} = \frac{\begin{pmatrix}\text{unit}\dagger\text{ for less}\\ \text{effective device}\\ \text{(from item 2)}\end{pmatrix} - \begin{pmatrix}\text{unit}\dagger\text{ for more}\\ \text{effective device}\\ \text{(from item 2)}\end{pmatrix}}{\text{Cost of more effective device}^{*} - \text{Cost of less effec tive device}^{*}}$$

$$\text{Number of years to liquidate} = \frac{\text{cost/unit saved}\dagger\text{/year}}{\text{dollars per hour paid trouble cre}}$$

Appendix C. How to Combin "Restoration Time" or Automotiv Mileage Values for Branch Pro tection, Figures 5 to 10 inclusiv With Those for Line Sectionalizin Figures 11 and 12

1. Where both cause "restoration time" automotive mileage greater than 100 p cent (the "yardstick"), add to the percen age on one curve the percentage above 10 per cent on the outer curve, that is:

Resultant per cent = (per cent on one curv + (per cent − 100 on othe

2. Where one system causes "restoratic time" or automotive mileage greater tha 100% and the other system less than 1 per cent, subtract from the first percenta the value of 100 − the second percentag that is,

Resultant per cent = (per cent which greater than 100 per cent) − (100 − per ce which is less than 100 per cent)

* The cost value should be either the initial cost the installed cost.

† The unit used may be in terms of restoration hou per year or automotive mileage per year.

Tests and Analysis of Circuit-Breaker Performance When Switching Large Capacitor Banks

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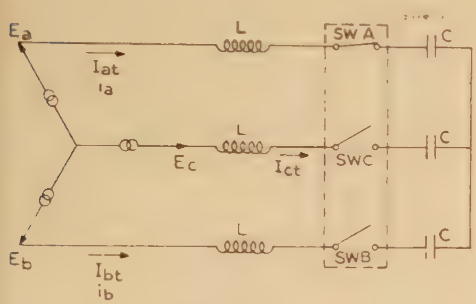
THE increased kilowatt burden on generation and transmission equipments—occasioned by the existing national emergency—calls for a reduction in the use of these equipments for generating and transmitting reactive kilovolt-amperes. When operating conditions permit the use of either capacitors or synchronous condensers, the relatively shorter production time of capacitors is making them more and more justifiable as a reactive source in the range of kilovolt-ampere ratings where economics might normally dictate a synchronous condenser.[1] At the present time the use of capacitors in banks up to 10,000 kva is being given increased consideration, and several installations having steps in this range have already been made.

With this growing use of capacitors, it is becoming increasingly necessary to apply adequate power circuit breakers to the task of switching these large banks, either totally or in steps, on and off the system. It has long been recognized that the job of switching capacitive circuits, such as long unloaded transmission lines, can become difficult, and it therefore logically follows that both the duty on the breaker and its effect on the system during switching of large capacitor banks should be investigated.



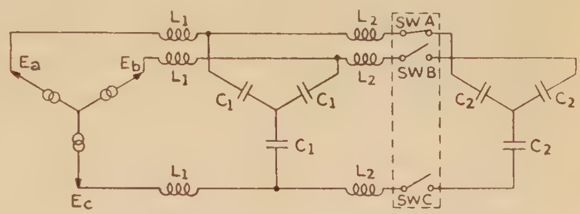


Figure 1. Transient-analyzer circuit for determining effects of sequential pole closing

(a). Total capacitor bank energized

(b). Capacitor bank energized against already energized bank. L_2 is inductance of breaker and bus work between banks

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When a capacitor bank is energized, a large component of natural frequency current initially flows in addition to the normal-frequency circuit current. If the bank is energized against an already energized bank, even larger natural-frequency equalizing currents may flow through the breaker. The effect of these currents must be evaluated.

When a capacitor bank is de-energized, larger than normal system voltages may exist across the breaker contacts because of the trapped charge on the capacitor. This may impose a greater than usual task on the breaker dielectric to interrupt with a minimum of restriking, and if restriking occurs, there may be voltage stresses above normal on the capacitor units as well as on the other connected equipment.

This paper offers an analysis of the problem together with test results on full-scale and miniature capacitor banks. Certain conclusions are drawn relative to the duty on and selection of breakers for this service.

Summary of Conclusions

A total of 338 tests on oil blast and Magne-blast types of power circuit breakers in the voltage class up to 15 kv and up to 250,000 kva short-circuit capacity, have been made. The results of this investigation, coupled with the analytical treatment contained in this paper, have allowed the following conclusions to be reached:

1. Based upon transient-analyzer analysis, it is shown that under the most pessimistic conditions of controlled restrike, abnormal circuit overvoltages can occur in de-energizing capacitors. This agrees with the results of other investigations[2] covering fault clearing on circuits containing capacitance.

2. Full-scale tests demonstrate that *standard* oil-blast breakers in the above classification recover their dielectric strength sufficiently fast to switch capacitor banks up to at least 10,000 kva without creating abnormal overvoltages.

As the capacitor kilovolt-amperes is further increased, a point is reached where restriking becomes more probable. Whether abnormal overvoltages appear or not is then determined by the randomness of the restriking phenomena and the inherent damping characteristics of the associated circuits.

3. Full-scale tests show that the high series resistance in the arc chute of the Magne-blast type of breaker, when restriking occurs, prevents the abnormal overvoltages usually associated with restriking phenomena. This is evidenced by the successful interruption of 31,400 kva—3,400 amperes at 5,350 volts.

4. Full-scale tests demonstrated that the peak values of transient equalizing currents upon energizing capacitor banks are within the values given by the equations presented in this paper and in reference 2.

5. The short-circuit interrupting rating of a breaker need not be increased by the

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3. Where both systems cause "restoration time" and automotive mileage less than 100 per cent (the "yardstick"), subtract from the percentage on one curve the value of 100 − percent on the other curve, that is,

Resultant per cent = (per cent on one curve) − (100 − per cent on other curve)

To determine the actual "restoration time" and automotive mileage multiply the "yardstick" in Table I, modified as directed in Figure 4, by the resultant percentage.

Reference

1. RELATIVE VALUE OF DIFFERENT TYPES OF OVERCURRENT PROTECTION FOR DISTRIBUTION CIRCUITS, G. F. Lincks. AIEE TRANSACTIONS, volume 61, 1942, January section, pages 19–26.

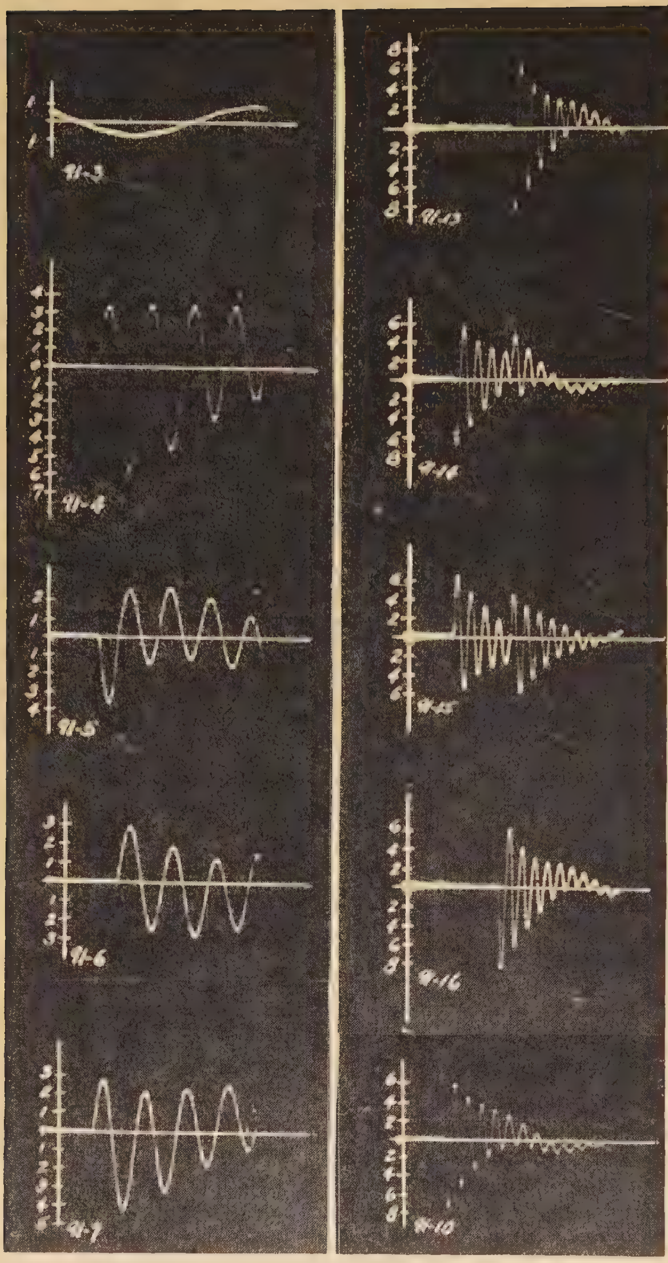


Figure 2. Transient-analyzer oscillograms showing currents obtainable when energizing capacitors

Current calibration in times normal steady-state capacitor current

91-3 One cycle of normal magnitude, fundamental frequency capacitor current

91-4 Inrush current, phase *b*, on energizing capacitor bank of Figure 1a. Switch *b* closed at E_{ab} maximum; switch *c* closed 36 degrees later for maximum I_b

91-5 I_a for condition of 91-4

91-6 I_c for condition of 91-4

91-7 Inrush current, phase *b*, on simultaneous energizing of capacitors of all three phases of Figure 1a at the instant that makes I_b maximum

91-10 Inrush current, phase *b*, on simultaneous energizing of capacitors of all three phases of Figure 1b at the instant that makes I_b maximum

91-13 Inrush current, phase *c*, on energizing capacitor bank of Figure 1b. Switch *b* closed at E_{ab} max; switch *c* subsequently closed to give I_c a maximum

91-14 I_b for condition of 91-13

91-15 I_a for condition of 91-13

91-16 Same as 91-13, except switch *b* closed 10 degrees ahead of E_{ab} max

Switch *c* subsequently closed to give I_c a maximum

proximity of a capacitor bank on the bus side.

6. All tests made under various energizing and discharging conditions—many of which are presented in this paper—showed that the peak values of transient current obtained were not harmful to the breakers tested. The maximum value reached was 52,000 amperes.

The Energizing Problem

Energizing a Single Bank

It has been shown[3] that the maximum inrush current, neglecting system capacitance and damping, when energizing a single capacitor bank at the peak of the voltage wave is given by

$$I_{\max}=I_0(1+\sqrt{X_c/X_L})$$

where

I_0=peak value of steady-state capacitor current

X_c=reactance of capacitor bank

X_L=system reactance viewed from capacitor location

Since a switching problem is being considered, it may be appropriate to express inrush currents to capacitors in terms of the short-circuit current available at the capacitor location. Doing this, the above expression may be written as

$$I_{\max}=I_c(1+\sqrt{I_{sc}/I_c})$$

where the subscripts stand for the capacitor rated current and the short-circuit currents—as initialed. All currents are expressed in peak values. This amounts to approximately 2,500 amperes for a 10,000-kva bank of capacitors closing on to a 13.8-kv system capable of delivering 250,000-kva short circuit.

These expressions hold for each phase of a wye-grounded bank on a grounded system. In wye floating neutral or delta-connected banks, however, the initial individual phase currents are a function of the order of pole closing. And since breakers probably do not close their contacts simultaneously, this case was investigated to determine if larger currents than those indicated by the above expression, could be obtained.

The analysis of this case*—as given in appendix B—indicates that the maximum current from this effect is 1.15 times that which can be obtained by simultaneous closing of contacts. The circuit of Figure 1(a) was set up in miniature on the transient analyzer[4] with the following constants:

L=0.234 henry
C=1 microfarad

For these values $\sqrt{X_c/X_L}=5.5$ hence $I_{\max}$ by above equation should be no greater than $1+5.5=6.5$ times normal. Oscillographic results are presented in Figure 2. For the equivalent single-phase or simultaneous closing case, oscillogram 91-7 shows a peak inrush current in phase *b* of 4.5 times normal. Oscillogram 91-4 shows I_b for switch *b* closed at E_{ab} a maximum and switch *c* closed 36 degrees later to give I_b a maximum. This maximum of approximately 6.0 times normal, and the 4.5 above indicates that even the small damping in the miniature setup greatly reduces the maximum obtainable.

Energizing a Bank Against an Already Energized Bank

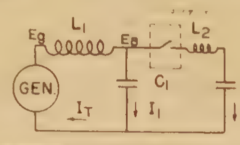
In this case the energized bank discharges into the oncoming bank through the relatively small impedance between them. The resulting large equalizing current of short duration oscillates at the high natural frequency determined by the value of series capacitance of the two banks, and the inductance of the connections between them.

Equations for determining these currents have already been given[3] for simul-

* A time of at least 1/4 cycle of natural frequency constitutes a severe nonsimultaneous pole-closing condition from this standpoint.

Table I. Single-Phase Closing Tests When Energizing a Bank Against an Already Energized Bank

o	X_L Ohms	C_1 Micro-farads	C_2 Micro-farads	E_B RMS Volts	I_1 RMS Amperes	I_T RMS Amperes	I_1 Peak Test	I_2 Peak Calculated	Closing Angle Degrees		Natural Frequency (Cycles Per Second)	L_2 Millihenry Calculated from Test
)il-Blast Breaker—250,000 Interrupting Kva												
1	6.72	17.7	17.7	15,150	101	210	*	8,940	68		8,100	0.0436
2	3.72	35.4	35.4	16,500	220	400	*	15,800	72		6,400	0.0349
3	2.12	70.2	66.7	15,130	400	827	13,000	18,200	88	(Figure 3)	3,900	0.0487
4	0.42	440	417	5,850	960	2,030	15,400	17,700	89		1,750	0.051
5	2.12	70.2	66.7	14,380	380	786	640	1,250	4		4,100	0.0440
6	0.92	440	417	7,800	1,280	3,200	1,380	2,000	6		1,300	0.0700
Iagne-Blast Breaker—150,000 Interrupting Kva												
7	0.42	440	417	5,850	960	2,400	12,400	14,400	65		1,440	0.051
8	0.42	440	417	5,850	960	2,400	14,800	18,220	90	(Figure 4)	1,650	0.051
9	0.42	440	417	5,850	960	2,500	5,700	9,250	34		1,500	0.0525
)	0.42	440	417	5,850	960	2,400	10,600	11,400	51		1,330	0.067

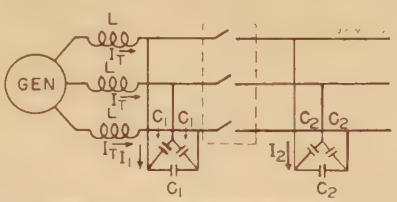


Natural frequency beyond vibrator response.

Table II. Closing Tests Energizing a Three-Phase Delta Bank Against an Already Energized Delta Bank

Tests Were Made at 5,400 Volts

o	X_L Ohm L-N	C_1 Microfarads L-L	C_2 Microfarads L-L	I_1 RMS Before Closing	I_2 Peak During Closing Pole$_1$	Pole$_2$	Pole$_3$	Natural Frequency (Cycles Per Second)	Per Cent Rise Before Closing	Per Cent Rise After Closing
1	0.805	133	126	540	8,800	9,500	3,800	1,800	16	31.0
2	0.805	133	126	540	8,000	9,100	2,700	2,000	16	31.0
3	0.805	133	126	540	8,400	9,300	2,700	1,800	16	31.0
4	0.805	133	126	540	8,000	8,500	5,100	2,000	16	31.0



aneous pole closing, so will not be repeated. The case of sequential pole closing, however, was set up in a miniature circuit of Figure 2, using the following constants:

$L_1 = 0.234$ henry $\quad C_1 = 3$ microfarads

$L_2 = 0.048$ henry $\quad C_2 = 1$ microfarad

(L_1 and L_2 in per unit of the total capacitive kilovolt-amperes are 0.133 and 0.027, respectively)

The equalizing current on energizing C_2 was measured by means of the cathode-ray oscillograph. A comparison of the effect of simultaneous closure of the three poles of the switch with that of sequential closure (with the interval between pole closures controlled to give maximum current) is given in Figure 2 by oscillograms 91-10 and 91-13. It will be noted that the first natural-frequency current peak is nearly the same (eight times normal) in the two cases, indicating that sequential closing for the adjacent bank case has less effect on current maximum than in the single bank case.

Oscillogram 91-16 shows the effect of closing the second switch b ten degrees ahead of the time when the voltage across it would be a maximum. The same maximum current (in phase c) was obtained

gure 3a. Oscillograms showing the equalizing currents associated with the energizing of an adjacent bank with an energized bank on the bus

See Figure and test 3 in Table I

B—Bus capacitor current, I_1 C—Cathode ray oscillograph relay trip
D—Oncoming capacitor current, I_2 E—Line current, I_T F—Breaker travel

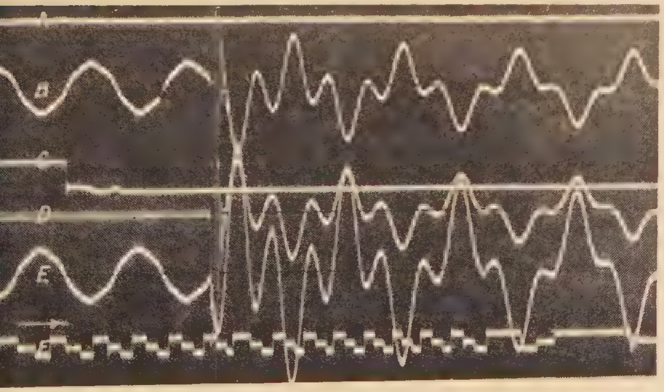
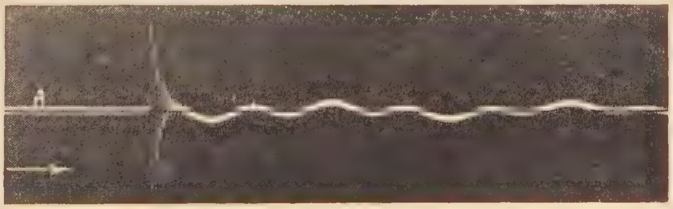


Figure 3b. Oncoming capacitor current of Figure 3a

See test 3 of Table I



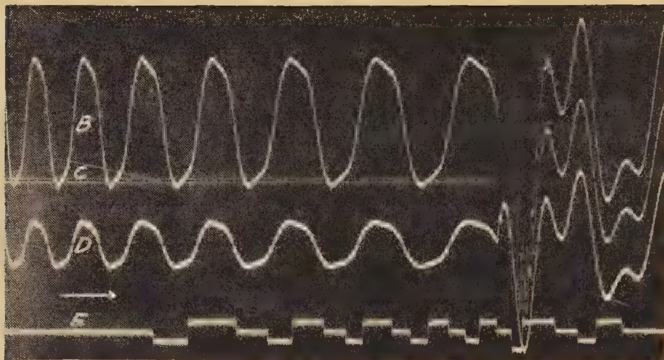


Figure 4a. Oscillogram showing the currents associated with the energizing of a 417-microfarad bank with a 440-microfarad bank on the bus

See test 8 in Table I

B—Bus capacitor current, I_1
C—Oncoming capacitor current, I_2
D—Line current, I_T
E—Breaker travel

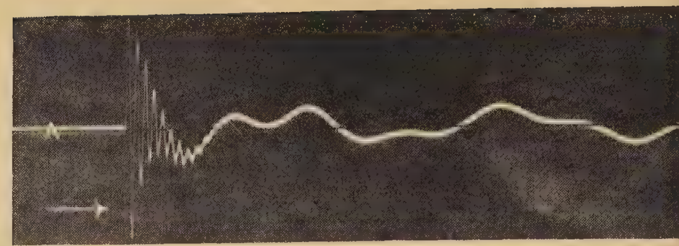


Figure 4b. Oncoming capacitor current of Figure 4a

See test 8 in Table I

by varying the angle of closing the last switch.

Full-Scale Tests

Tables I and II list a few of the typical test data recorded during the energizing tests. The test arrangement was designed to have small inductances in order to obtain maximum currents. In spite of this, the maximum current measured was below 20,000 amperes. In none of these tests were current-limiting impeders used. Figures 3, 4, and 5 are typical oscillographic records of the energizing phenomena with cross reference to Tables I and II giving the numerical values.

Discussion

The data in Tables I and II show the equalizing current values of adjacent single-phase and three-phase bank energizing. The largest adjacent bank energizing currents are well below the nominal values of current which these breakers are normally called upon to handle. The successful performance of the breakers under these conditions, together with the results presented in the section on "Capacitor Discharge During Short Circuit," shows that the "closing-in" duties of standard breakers constitute a very minor problem. The equalizing currents are high-frequency (1,000 to 5,000 cycles) highly damped discharges, producing negligible distress either as contact burning or as a delaying mechanical impulse to the closing breaker contact. Figure 6 shows an enlarged portion of the breaker contacts after 34 closing tests.

The De-energizing Problem

The initial voltage *across* a circuit breaker following the interruption of a capacitor circuit is zero, since the capacitor on one side holds the same crest voltage that existed on the bus side of the breaker at and immediately following current zero. This permits any circuit breaker to interrupt quite easily at the first arc current zero. The breaker is thus lulled into a feeling of easy triumph over the circuit with the minimum of contact separation when suddenly—with the cyclic reversal of system voltage—double instead of zero voltage appears across the open breaker just one-half cycle following the interruption. If the breaker withstands this double voltage without restriking, then and not until then can the circuit be considered as cleared. If, on the other hand, the circuit is restruck as the voltage approaches this cyclic reversal, then a series of events is possible which must receive special consideration.

In counterdistinction to capacitive interruptions, the current in inductive circuits is interrupted at each current zero, whereupon the double-voltage-recovery transient is immediate, and circuit interruption is either achieved, or the arc is restruck for another half cycle to repeat the attempt under similar conditions at the next current zero. Thus the insulation is permitted to build up between the separating contacts until such a point is reached that the circuit is cleared without accumulative voltage distress. (There

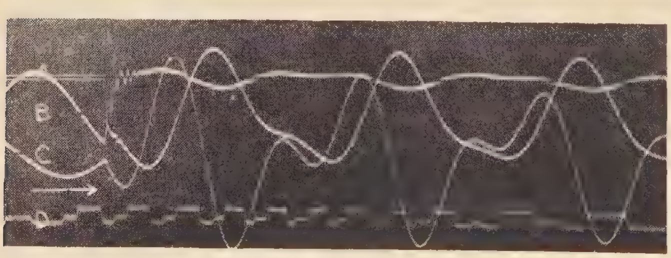
Figure 6. Circuit breaker contacts following a total of 34 closing tests in which an adjacent bank was energized with an energized bank on the bus

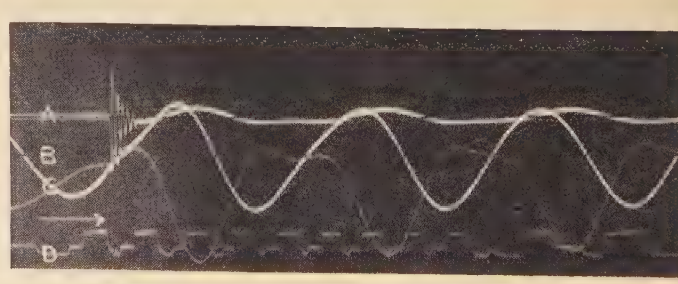
Typical tests are shown in Table I

Figure 5 (below, right and left). Oscillograms showing the phenomena in two phases associated with the closing on a 4,150-kva delta bank with a 4,400-kva bank on the bus

See test 4 of Table II

A—Capacitor current, I_1
B—Line-to-line voltage
C—Bus side capacitor current, I_2
D—Breaker travel





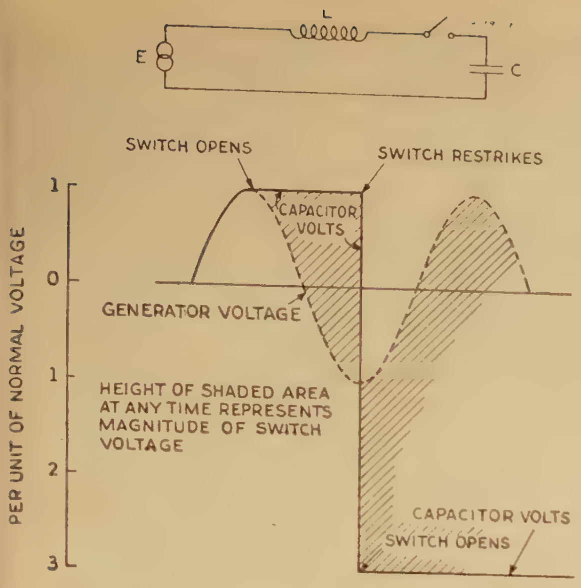


Figure 7 (left). Case I—De-energizing grounded neutral capacitor bank

Total capacitance switched—shows basis of analysis

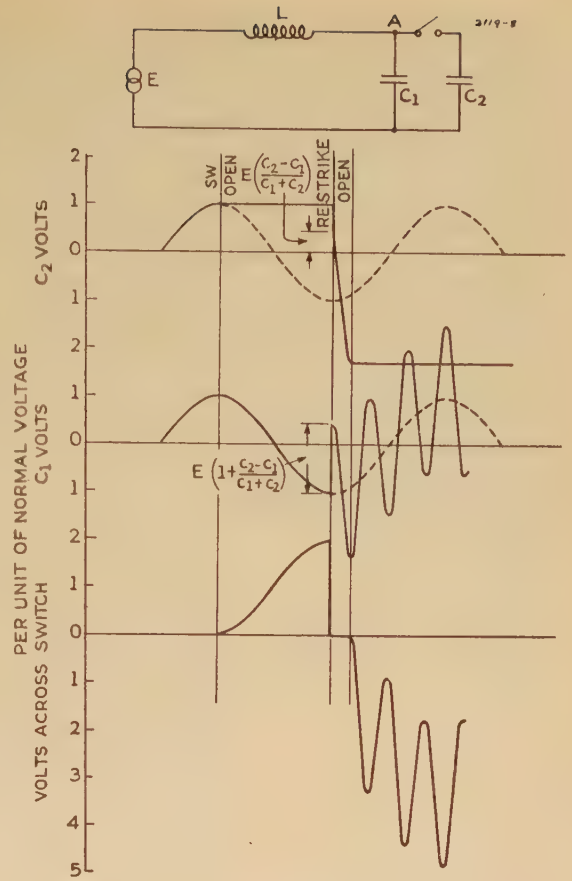


Figure 8 (right). Case II—De-energizing grounded neutral capacitor bank

Part of capacitance unswitched $C_1/C_2 = 0.2/0.5$

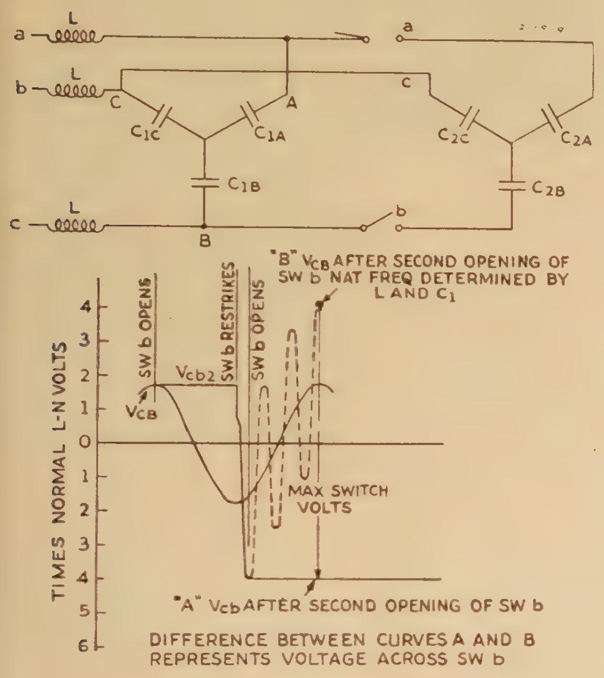


Figure 9. Case VIII—De-energizing ungrounded neutral three-phase capacitor bank with part of capacitance unswitched

Second switch to open

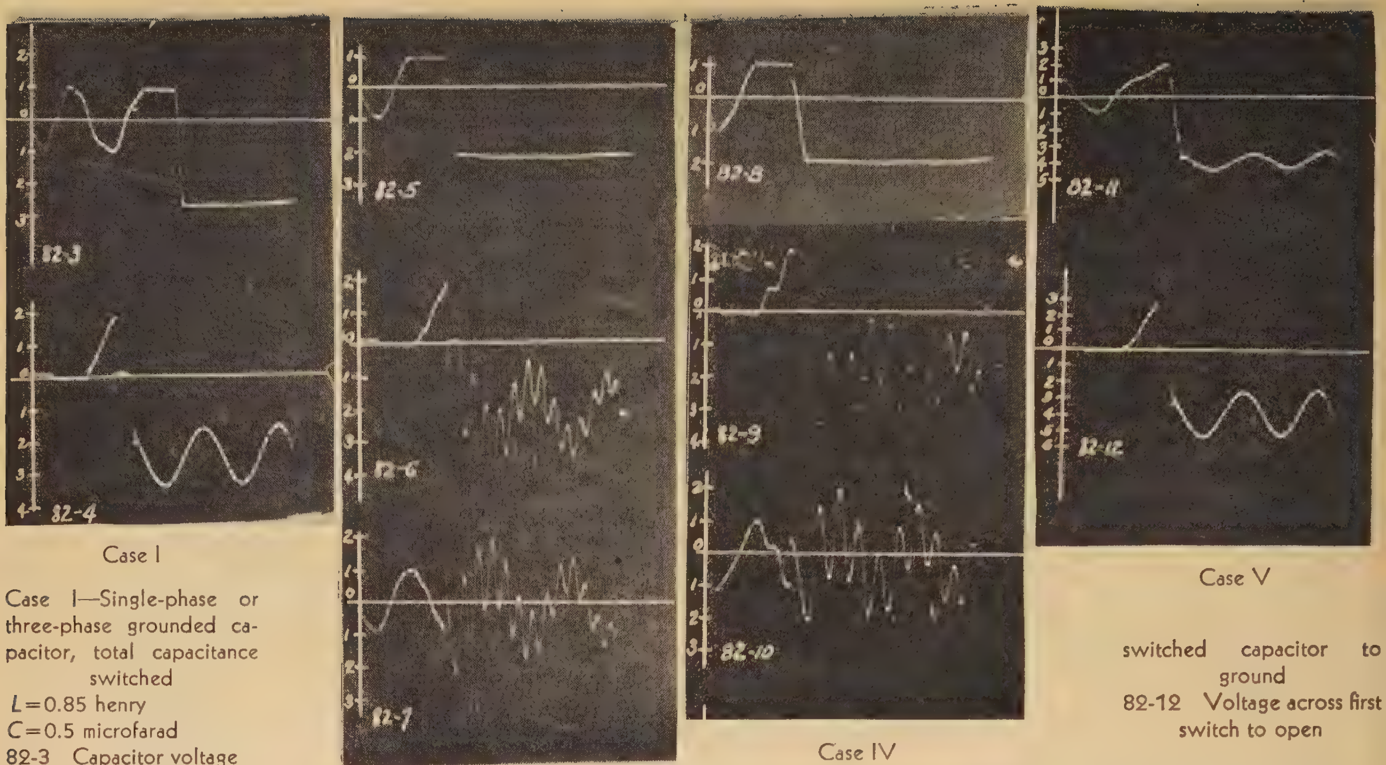
are however special cases of inductive circuits where abnormal restriking overvoltages can occur.[2] The effects of the restriking phenomenon on the capacitor switching problem are discussed under the section "Capacitor Discharge During Short Circuit.")

A comparison of these phenomena indicates that the interrupting capacity of standard breakers on capacitive circuits may be limited as described in this paper. The limitations are not so much in the ability of the breakers to eventually clear the circuit as they are in the desirability of limiting overvoltages on the system to safe values.

At this point, it is desirable to point out that the considerations contained in this paper deal with capacitor banks of high capacitive kilovolt-amperes, and in no way should the discussion presented here be construed to apply to circuits which contain relatively small capacitors—as for example, capacitors for the protection of electric equipment from lightning or switching surges and the lower-voltage equipments in industrial plants. In fact, the phenomena presented here are more akin to the phenomena of the interruption of line-charging current on high-voltage transmission systems. The two problems are identical in every major respect and differ only in degree. The capacitive kilovolt-amperes to be interrupted on high-voltage lines is usually large. As with the capacitor bank, interruption takes place easily at the first current zero in the arc, thus leaving the entire line charged to the crest value of generated voltage. As the generated voltage reverses on the bus side, double voltage appears across the switch. The breakers required to interrupt this line-charging current are inherently large, and although the contact velocity on parting is considerably larger than in the smaller breakers, it is not larger in proportion to the voltage. It follows that the voltage to appear across the open pole one-half cycle after contact parting is therefore much greater on the high-voltage systems. This fact, together with the large kilovolt-amperes to be interrupted explains why these interesting phenomena were first found on the high-voltage transmission systems. The trend toward large capacitor banks tends to project the above phenomena into the lower-voltage systems, not by reason of voltage, but because the heavier currents to be interrupted act to weaken the arc gap one-half cycle after parting, thus permitting the gap voltage on the reversal of bus voltage more easily to produce a restrike. The studies presented in this paper reveal the degree to which these phenomena are important to the lower-voltage switching equipments.

Analysis

Tests have been made on the transient analyzer[4] to determine the maximum magnitude of transient overvoltage that would be obtained when switching off various connections of capacitors, with the assumption that the circuit-opening mechanism would restrike once. The capacitor switch was opened at a funda-



Case I

Case I—Single-phase or three-phase grounded capacitor, total capacitance switched
L=0.85 henry
C=0.5 microfarad
82-3 Capacitor voltage
82-4 Voltage across switch

Case II—Single-phase or three-phase grounded capacitor, part of capacitance switched
L=0.85 henry
C_1 (unswitched)=0.2 microfarad
C_2 switched=0.5 microfarad

Case II

82-5 Capacitor voltage
82-6 Voltage across switch
82-7 Unswitched capacitor voltage

Case IV—Same as case II, except L=1.67
C_1=0.2 microfarad

Case IV

C_2=0.5 microfarad 82-8
82-9
82-10

Case V—Three-phase ungrounded neutral capacitor, total capacitance switched—first switch to open, phase a
82-11 Voltage of

Case V

switched capacitor to ground
82-12 Voltage across first switch to open

Figure 10. Transient-analyze oscillograms showing voltage obtainable when de-energizing capacitors, for various connections and assuming one restrike at peak of voltage wave

All voltage calibrations in times normal of normal L-N crest voltage

mental frequency current zero and reclosed by means of a synchronous switch at the subsequent recovery voltage maximum to represent a restrike. Although this switching procedure does not involve the randomness present in the circuit-breaker performance, it gives the highest possible magnitudes of both switch and capacitor voltage.

Table III presents the results of these tests, giving the peak switch and capacitor volts for the condition of no restrike and one restrike. Figures 7 through 9 show some of the connections tested and a "free-hand" graphical analysis of the voltages. These supplement the oscillograms in Figure 10.

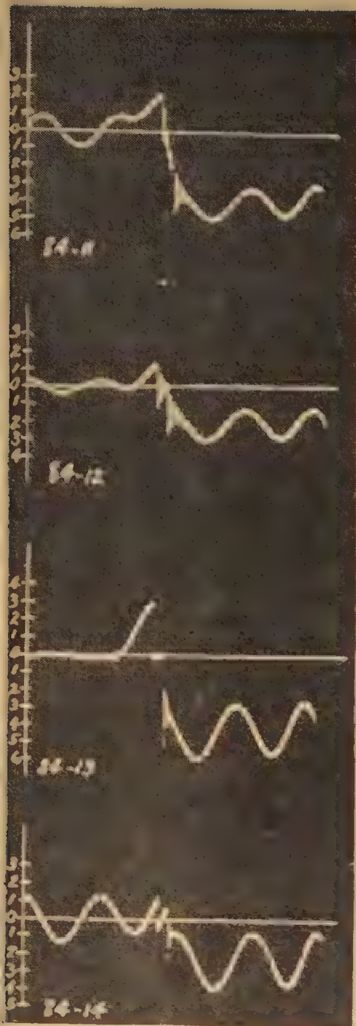
Figure 7 shows the fundamentals by which these transient overvoltages are obtained. At a current zero (peak of the voltage wave) the switch is opened, whereupon a full charge remains on the capacitor. The fundamental frequency system voltage moves onward until it is a negative maximum resulting in a switch voltage of twice normal. A restrike is permitted at this point, and the capacitor voltage changes to system voltage on the circuit natural frequency and overshoots (neglecting resistance damping) an amount equal to its attempted change, or to −3 times normal, as shown. At the first subsequent current (largely natural frequency) zero, the switch is reopened, and the charge of −3 times normal remains on the capacitor.

Figure 8 shows a similar analysis for switching off part of a bank (65 per cent of the total in all these tests) with one restrike. At the time just previous to restrike, C_1 is at −1.0 per unit volts and C_2 is at +1.0 per unit volts. In the transient-analyzer tests there was no intentional current limiter between C_1 and C_2; hence immediately upon restrike the charge on C_1 and C_2 equalized to a value

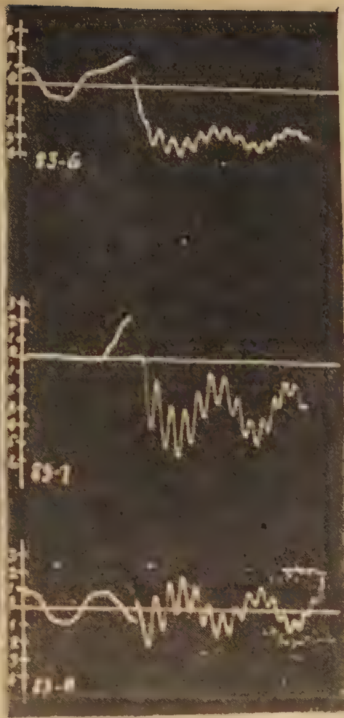
Table III. Transient Analyzer De-energizing Tests

Case No*	Condition	Peak Capacitor Volts to Ground: No Restrike	Peak Capacitor Volts to Ground: One Restrike	Peak Switch Volts: No Restrike	Peak Switch Volts: One Restrike
Grounded Neutral					
I	Switching 0.5-microfarad bank	1.0	3.0	2.0	4.0
II	Switching 0.5-microfarad bank, 0.2 microfarad unswitched	1.0	2.4	2.0	4.8
III	Same as I, L_1 changed from 0.85 to 1.67 henries	Similar to case 1			
IV	Same as II, L_1 changed from 0.85 to 1.67 henries	Similar to case 2			
Ungrounded Neutral					
V	Switching 0.5-microfarad bank, 1st phase	2.0	5.0	3.0	6.0
VI	Switching 0.5-microfarad bank, 2nd phase	2.6	6.0	3.46	6.6
VII	Switching 0.5-microfarad bank, 0.2-microfarad unswitched 1st phase	2.0	3.7	3.0	6.1
VIII	Switching 0.5-microfarad bank, 0.2-microfarad unswitched 2nd phase	2.5	6.0	3.46	8.2
IX	Same as VII, neutrals of capacitors connected	1.6	3.3	2.6	4.3

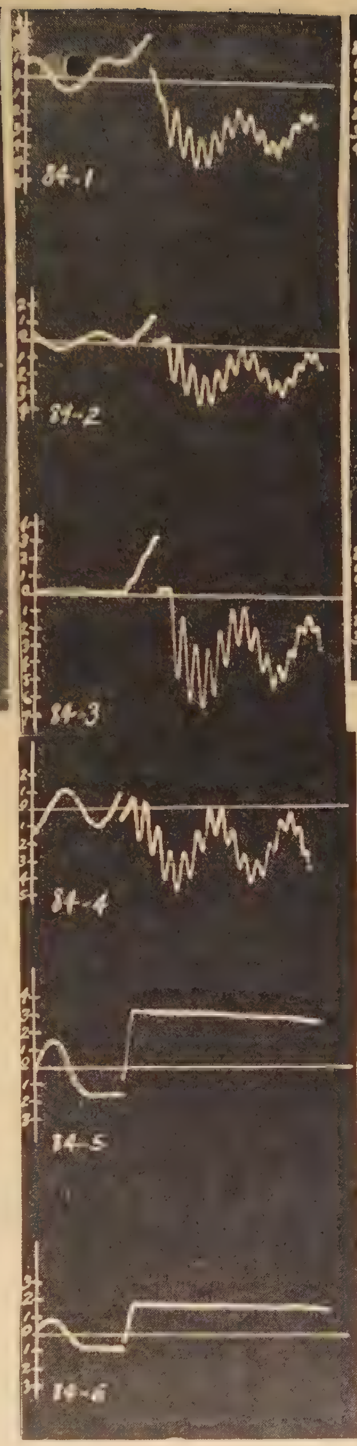
* See oscillograms, Figure 10.



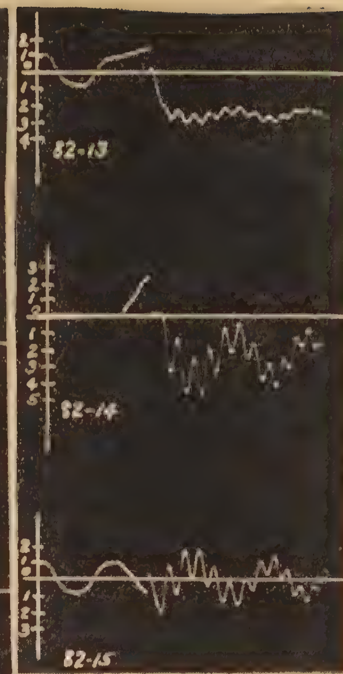
Case VI



Case VII



Case VIII



Case IX

Case VI—Same as case V Second switch to open (*b* phase)

84-11 Voltage of capacitor being switched (*b*) to ground

84-12 Voltage of capacitor already switched (*a*) to ground

84-13 Voltage across switch *b*

84-14 Voltage across switch *a*

Case VII—Three-phase ungrounded neutral capacitor, part of capacitance switched—first switch to open

83-6 Voltage of switched capacitor to ground

83-7 Voltage across first switch to open

83-8 Voltage of unswitched capacitor on phase *a* to ground

Case VIII—Same as case VII. Second switch to open (*b* phase)

84-1 Voltage of capacitor being switched (phase *b*) to ground

84-2 Voltage of capacitor already switched (*a*) to ground

84-3 Voltage across switch *b*

84-4 Voltage across switch *a*

84-5 Voltage V_{bc} on opened capacitors

84-6 Voltage V_{BC} on unswitched capacitors

Case IX—Same as case VII. First switch to open, capacitor neutrals connected. (Comparison with case VII shows effect of unswitched capacitors in holding down neutral displacement of switched capacitors)

82-13 Voltage of switched capacitor to ground (phase *a*)

82-14 Voltage across first switch to open

82-15 Voltage of unswitched capacitor on phase *a* to ground

Figure 10 (continued.) Transient-analyzer oscillograms showing voltages obtainable when de-energizing capacitors, for various connections and assuming one restrike at peak of voltage wave

All voltage calibration in times normal of normal *L-N* crest voltage

given by $E(C_2-C_1)/(C_1+C_2)$ and then oscillated to a value given by $-E(C_1+3C_2)/(C_1+C_2)$. An analytical determination of this expression is given in appendix B.

The constants used for these transient-analyzer tests—in *L-N* values—were:

System inductance $L=0.85$ henry except cases 3 and 4, $L+1.67$ henries

Capacitance switched $C_2=0.5$ microfarad

Capacitance unswitched $C_1=0.2$ microfarad

The most severe overvoltage across the switch on de-energizing occurs when switching off part of an ungrounded wye (or delta bank) and when the second phase clears. This is shown by Figure 9 (case VIII). The capacitor overvoltage is equal to that obtained when switching all of the bank.

Full-Scale Tests

Analysis shows—Table III—abnormal voltages are possible with one restrike under the most pessimistic conditions. Full-scale tests were made to determine practical conditions obtaining.

Table IV. De-energizing Tests—Three-Phase Ungrounded Capacitor

No	E_G Volts L-L	X_L Ohms L-N	C Micro-farads L-L	Capacitive Kva Switched	E_B Peak Opening L-G		E_B/E_G L-G Peak Opening	I_{RMS} Opening Amperes	Remarks
Oil-Blast Breaker—150,000 Interrupting Kva									
1	4,000	0.23	300	6,330	4,000		1.23	845	No disturbance—cleared
2	4,000	0.23	600	15,100	4,100		1.26	1,850	
3	4,000	0.23	600	15,100	4,000		1.23	1,850	
Oil-Blast Breaker—250,000 Interrupting Kva									
4	14,500	3.38	17.7	4,850	16,600		1.40	180	No disturbance—cleared
5	14,500	3.38	17.7	4,850	16,600		1.40	180	
6	14,500	3.38	34.4	10,800	16,000	Figure	1.35	375	
7	14,500	3.38	34.4	10,800	19,400	12	1.64	375	
8	14,500	3.38	34.4	10,800	19,000		1.61	375	
Magne-Blast Breaker—150,000 Interrupting Kva									
9	4,000	0.23	600	15,200	5,000		1.54	1,850	Moderate report—cleared
10	4,000	0.23	300	6,300	6,000		1.85	845	
11	4,000	0.23	975	31,400	4,900		1.51	3,400	
12	4,000	0.23	975	31,400	5,000		1.54	3,400	
Magne-Blast Breaker—250,000 Interrupting Kva									
13	5,400	0.47	259	11,400	6,000		1.37	1,050	Moderate report—cleared
14	5,400	0.47	259	11,400	6,500	Figure	1.49	1,050	
15	5,400	0.47	259	11,400	5,100	11a	1.17	1,050	
16	5,400	0.47	259	11,400	5,400		1.23	1,050	

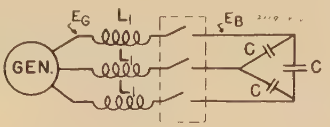
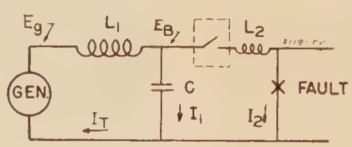


Table V. Closing–Opening Tests in Which Capacitor Discharged Through Breaker on Initiation of Fault

For These Tests Capacitance = 857 Microfarads

No	X_L Ohm	E_g Volts	E_B Volts	I_1 RMS Before Closing	I_1* Peak on Closing (Measured)	I_1 Peak on Closing (Calculated)	I_1 Peak on Opening	I_2 Short-Circuit Peak on Closing (60 Cycles)		I_2 RMS Symmetrical Fault (60 Cycles)	Switching Angle Degrees	Natural Frequency (Cycles Per Second)	L_2 Millihenry Calculated From Test
Magne-Blast Breaker													
1	0.421	5,400	6,300	2,050	10,000	11,100	8,800	36,000		11,800	12	1,100	0.0244
2	0.421	5,400	6,300	2,050	42,000	47,000	9,700	25,000		11,700	62	1,100	0.0244
3	0.421	5,400	6,300	2,050	7,800	7,400	9,400	36,000	Figure 13b	11,800	8	1,100	0.0244
4	0.421	5,400	6,300	2,050	52,000	53,000	9,500	20,000	Figure 13a	11,800	86	1,100	0.0244
Oil-Blast Breaker													
5	0.421	5,400	6,300	2,050	38,000	44,000	8,450	27,000		10,900	48	1,220	0.020
6	0.329	5,400	6,040	1,950	8,000	8,350	8,500	45,000		14,400	9	1,160	0.022
7	0.329	5,400	6,040	1,950	37,000	44,200	8,750	32,000		14,500	56	1,160	0.022



* This peak current flows through breaker in addition to short-circuit current, but has disappeared with the arrival of peak short-circuit current.

The typical test results summarized in Table IV indicate the scope of these tests. Oil and Magne-blast breakers of the voltage class up to 15 kv and up to 250,000-kva interrupting capacity were tested. It is interesting to note that the Magne-blast breaker successfully interrupted 31,400 kva at 4,000 volts without disturbing overvoltage in spite of some restriking. This result is caused by the high series resistance present[5] when restriking occurs within the arc chute, see Figure 11a. This desirable characteristic of the Magne-blast type of breaker places it in a very favorable position when switching capacitive circuits. Figure 11 exhibits typical cathode-ray oscillograms of the voltages attending these interruptions.

The 15-kv, 250,000-interrupting-kva oil breaker was tested up to approximately 11,000 kva with results as tabulated. Figure 12 shows a typical recording of this series of tests when de-energizing the larger bank ratings.

Discussion of Results

The transient-analyzer approach allowed the circuit constants to be readily varied for determining generalized qualitative results, thus materially reducing the full-scale test requirements. Also, with complete control of the random phenomenon called "restriking," certain conclusions could be drawn with respect to the maximum possible overvoltages obtainable under the most pessimistic conditions, which again helped to plan and analyze the full-scale program. The miniature setup allowed the following conclusions to be drawn:

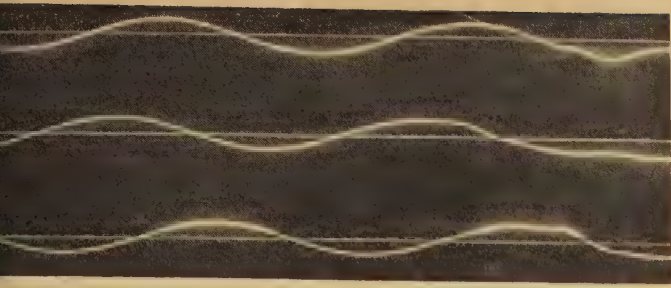
1. Effect of circuit constants (L and C). If the entire bank is switched, the magnitude of the switch or capacitor voltage is not appreciably affected by the system constants provided that the circuit natural frequency is not less than approximately 180 cycles. (Refer to cases I and II of Figure 10.)

2. Presence of an unswitched bank. If part of the bank remains unswitched, the capacitor and switch volts are the same as the condition of total bank switched, if no restrike occurs. If restrike occurs, the smaller the bank unswitched, the larger the switch voltage, the limit being a very small unswitched capacitance for which a somewhat greater voltage may be obtained than for the case of absolutely no unswitched capacitance. Table I summarizes quantitative results.

3. Effect of grounding. Grounding the capacitor bank on a grounded system results in the lowest switching overvoltages, as may be seen by comparing the values of the grounded and ungrounded cases of Table III.

4. Effect of interconnecting neutrals of switched and unswitched banks. The unswitched bank under this condition has the tendency of holding the neutral of the switched bank at ground potential, and as the ratio of unswitched to switched capacity increases, the grounded case is more nearly approached. (Refer to cases VII and IX of Figure 10.)

Full-scale tests demonstrated that delta-connected or wye floating neutral capacitors up to 10,000 kva can be handled with standard breakers without creating the abnormal overvoltages indicated by the analysis. This appears to substantially satisfy present-day system requirements. Above these values a



igure 11a. Cathode-ray oscillogram show-ng the capacitor voltages to ground attending he interruption of a three-phase 6,500-kva ank with the Magne-blast air circuit breaker

See test 14 on Table IV

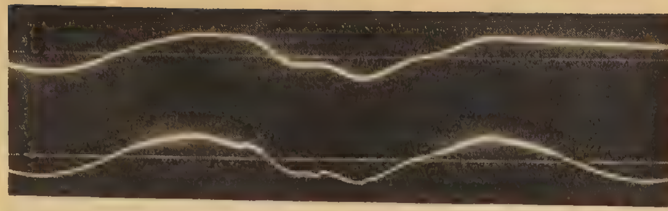


Figure 11b. Cathode-ray oscillogram of voltages to ground associated with the Magne-blast breaker opening a single-phase 3,300-kva 5,400-volt bank of capacitors

B—Capacitor volts
C—Bus volts

oint is reached where restriking to-ether with the attendant overvoltages ecomes more probable, thus presenting field for further development should the equirement greatly expand.

apacitor Discharge During Short Circuit

When a short circuit occurs on the load ide of a large capacitor bank, the capacitor discharges its energy into the fault—in some applications—along with the flow of short-circuit current. (See figure in Table V.) The question naturally arises regarding the extra duty, if any, required of the circuit breaker through which both of these currents flow. A study of the problem reveals that the most severe case of the above consideration concerns the closing of the breaker upon such a fault with immediate opening. The concern regarding this case is dispelled when it is realized that the maximum capacitor energy is "dumped" when the fault is initiated at the crest value of the system voltage. Under these conditions, the short-circuit current which flows is at its lowest value, namely the symmetrical value. In this case the highly damped capacitor discharge is practically over with the arrival of the first crest of normal-frequency short-circuit current. See Figure 13a.

If, on the other hand, the maximum short-circuit current is developed by initiating the fault at the zero point of the voltage wave, the capacitor charge is zero and hence contributes nothing. See Figure 13b.

Analysis shows that this latter case develops the maximum thermal and magnetic effects. Since this is a normal function of the breaker, it follows that the presence of the capacitor cannot add to the maximum closing duty of the breaker. It should be pointed out, in

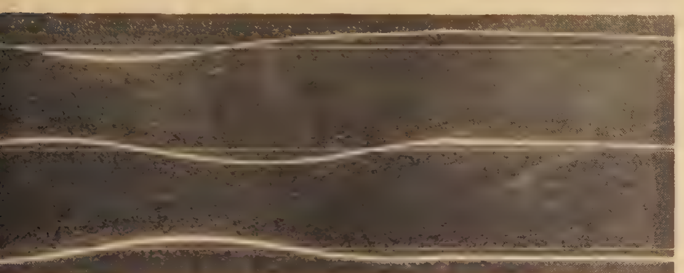


Figure 12. Cathode-ray oscillogram showing the capacitor voltages to ground associated with the interruption of a three-phase 10,800-kva bank with an oil-blast circuit breaker

See test 6 on Table IV

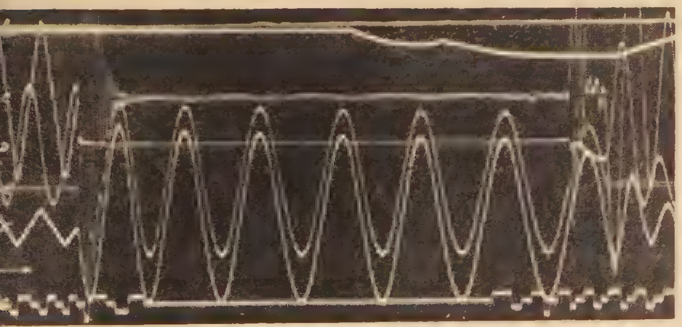


Figure 13a. Fault initiated near the peak of generated voltage

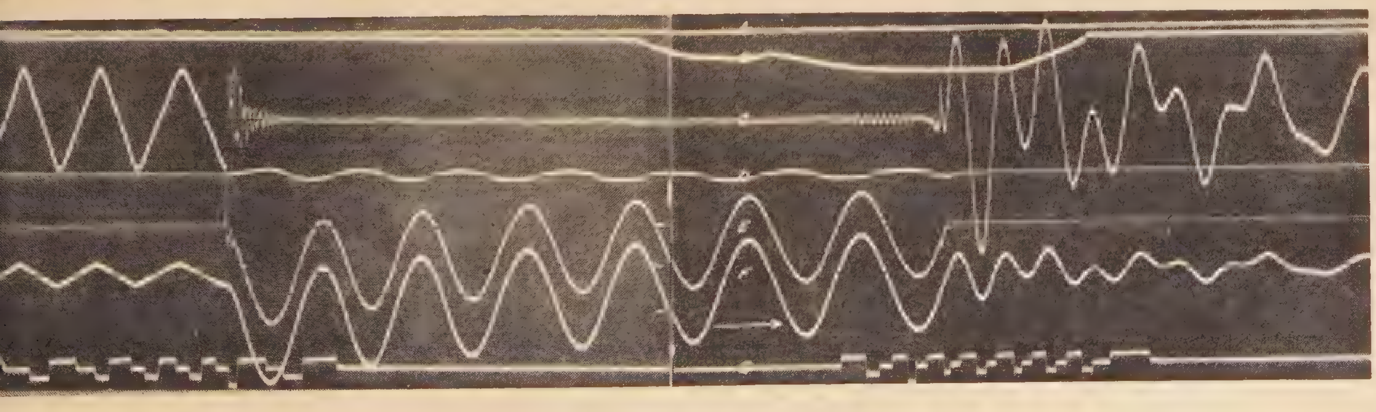
Maximum capacitor charge. Note the high capacitor discharge in the presence of approximately symmetrical fault conditions. See test 4, Table V

Figure 13 (left, below). Typical oscillograms showing the currents resulting when a circuit breaker closes upon and interrupts short circuit when a large capacitor bank is present on the bus

B—Trip-coil current
C—Capacitor current, I_1
E—Fault current, I_2
F—Line current, I_T
G—Breaker travel

Figure 13b. Fault initiated close to the minimum point of generated voltage

Minimum capacitor charge. Note the low capacitor contribution in the presence of the displaced short-circuit current. See test 3, Table V



addition to the above, that the presence of the shunt capacitor on the bus materially aids the circuit breaker in its duty by lowering the rate of rise of recovery voltage. This last deduction does not hold if the breaker is attempting to clear a L-G fault on a system that is susceptible to high arcing-fault voltages. Other[2] investigations indicate that on these systems the reduction of the ratio of capacitive zero-sequence reactance to the positive-sequence inductive reactance tends to give the more severe overvoltages if restrike occurs. These conditions however, are rare and can be eliminated by properly grounding the system. In order to determine breaker operation when performing this function, as well as an attempt to get higher inrush currents in the interests of completeness, tests were conducted in which short circuits were produced by closing breakers upon the fault and tripping with no time delay. Two oscillograms are shown demonstrating the above relationships. Figure 13a shows a fault initiated near the peak of generated voltage. Note that the high-frequency capacitor discharge is so short that, with the arrival of the first peak of the symmetrical short-circuit current, little of the capacitor discharge remains. Figure 13b shows a fault initiated near the zero of generated voltage, giving rise to a displaced fault. Note the insignificant contribution made by the capacitor. Table V shows a partial list of the tests made under these conditions. The above considerations show that the interrupting duty on a circuit breaker is not increased by the presence of a large shunt bank on the bus side. In no case did the breakers in this series of tests exhibit abnormal stress.

Appendix A. Sequential Energizing of a Three-Phase Ungrounded Wye Capacitor Bank—Inrush Current

Considering the circuit given by Figure 1a, switch a may be closed with no electrical effect. The maximum current in phase ab may be obtained by closing switch b at a time when the voltage across it, E_{ab}, is a maximum. Thus

$$i_a=-i_b=\frac{\sqrt{3}E\cos\omega t}{2\left(pL+\frac{1}{pc}\right)}=\frac{(1-a)E_a}{2Z(p)} \tag{1}$$

where E=line-to-neutral generated voltage in phase a.

The voltage across the open switch c is

$$a^2E_a-\{E_a-i_aZ(p)\} \tag{2}$$

which with equation 1 substituted is

$$E_a\left(a^2-1+\frac{1}{2}-\frac{a}{2}\right)=\frac{3a^2}{2}E_a=-1.5E\sin\omega t \tag{3}$$

It is interesting to note that the voltage across the last switch to close has no natural-frequency component, even though the current flowing in phases b and c at this time does have. This may be written

$$E_{sw_c}=-1.5E\sin(\omega t'+\theta) \tag{4}$$

where

$\theta=\omega\Delta t$, and Δt is the time elapsed between the closing of switches b and c. Considering the effect of this voltage by itself,

$$i_c=\frac{-1.5E\sin(\omega t'+\theta)}{1.5Z(p)} \tag{5}$$

At any time after Δt the total current in phases a and b is

$$I_{aT}=i_a-\frac{i_c}{2}$$

$$I_{bT}=-i_a-\frac{i_c}{2} \tag{6}$$

Since I_{bT}, the total current in phase b, will be the largest, the operational expression for I_{bT} given by equation 6 will be solved.

$$-i_{bT}=\frac{\sqrt{3}E\cos\omega t}{2Z(p)}-\frac{E\sin(\omega t'+\theta)}{2Z(p)} \tag{7}$$

The solution of this is

$$I_{bT}=I_0\left[\frac{\sqrt{3}}{2}\sin\omega t-\frac{\sqrt{3}}{2}\sqrt{X_C/X_L}\sin\omega_0 t+\frac{1}{2}\cos(\omega t'+\theta)-\frac{1}{2}\cos\theta\cos\omega_0 t'+\frac{1}{2}\sqrt{X_C/X_L}\sin\theta\sin\omega_0 t'\right] \tag{8}$$

where

$I_0=\dfrac{E}{Z}$=normal capacitor current per phase

$X_C=\dfrac{1}{\omega C}\qquad X_L=\omega L$

$\omega_0=\omega\sqrt{X_C/X_L}$=natural frequency of the circuit

t terms are components of i_b, the current before switch c is closed

t' terms are components of $\dfrac{i_c}{2}$ the additional current after switch c is closed

Since the natural-frequency components of equation 8 have large coefficients by comparison with the forced frequency components, the latter may be neglected to simplify the task of determining the value of θ which will give the largest total component of current in phase b. The natural-frequency components are

$$I_{NF}=\sqrt{X_C/X_L}\left[-\frac{\sqrt{3}}{2}\sin\omega_0 t+\frac{1}{2}\sin\theta\left(\sin\omega_0 t\cos\frac{\omega_0}{\omega}\theta-\cos\omega_0 t\sin\frac{\omega_0}{\omega}\theta\right)\right] \tag{9}$$

Putting $dI_{NF}/d\theta$ equal to zero permits the value of θ to be found which will give maximum I_{NF}. This will not be far from that which will put the two components of equation 9 inphase which, for a natural frequency of 5 is $\theta=180/5=36$ degrees.

If $\theta=36$, $I_{NF}\max=\sqrt{X_C/X_L}\,1.12$

If $\theta=40$, $I_{NF}\max=\sqrt{X_C/X_L}\,1.18$

Adding 1.0 to $\sqrt{X_C/X_L}\,1.18$ to allow for fundamental frequency components of equation 9 gives

$$I_{bT}=I_0(5.9+1.0)=6.9I_0$$

which is 15 per cent higher than $I_{\max}$ determined for simultaneous switching.

Appendix B. Capacitor De-Energizing Analysis; Capacitor Voltage upon Restrike

Case 2. Part of Bank Unswitched

Referring to Figure 8, just before restrike, at $t=0$, the voltage existing across the switch $=-E(1+\cos\omega t)$ neglecting the rise in voltage of point a due to i, flowing through L. The effect of closing the switch (restriking) may be obtained by applying this voltage to the operational impedance of the circuit, and the switch current is

$$i_2=EC_2\frac{\omega_0^2}{\omega_1^2}\left[\frac{p(\omega_1^2+p^2)}{(\omega_0^2+p^2)}+\frac{p^3(\omega_1^2+p^2)}{(\omega_0^2+p^2)(\omega_2^2+p^2)}\right] \tag{10}$$

where

E=crest value of generated voltage

$$\omega_1^2=\frac{1}{LC_1},\quad \omega_2^2=\frac{1}{LC_2},\quad \frac{1}{\omega_0^2}=\frac{1}{\omega_1^2}+\frac{1}{\omega_2^2}$$

The change of voltage of capacitor C_2 due to the restrike is $\Delta e_{c_2}=i_2/pC_2$ for which the solution is

$$\Delta e_{c_2}=E\frac{\omega_0^2}{\omega_1^2}\left[\frac{\omega_1^2}{\omega_0^2}+\quad-\frac{\omega_1^2}{\omega_0^2}+1+\left(\frac{\omega_1^2-\omega_0^2}{\omega^2-\omega_0^2}\right)\cos\omega_0 t+\frac{\omega_1^2-\omega^2}{\omega_0^2-\omega^2}\cos\omega t\right] \tag{11}$$

If the natural frequency ω_0 is assumed very large compared with ω, then at $t=\pi/\omega_0$

$$\Delta e_{c_2}=2E\left[\frac{C_1+2C_2}{C_1+C_2}\right] \tag{12}$$

and the capacitor voltage after restrike is

$$E_{c_2}=E-\Delta e_{c_2}=E\left[\frac{-C_1-3C_2}{C_1+C_2}\right] \tag{13}$$

For case II, where $C_1=0.2$ microfarad and $C_2=0.5$ microfarad, $E_{c_2}=(-17/7)E$. This value may be obtained by conservation of charge—the sum of the charges on the capacitors after restrike is the same as before, hence immediately after restrike

E of both capacitors $=$

$$\frac{Q_T}{C_T}=\frac{(-C_1+C_2)E}{C_1+C_2}=-\frac{3}{7}E$$

The equalized capacitor voltage must still change through $2C_2/C_1+C_2\,E$ to arrive at the

Application of Vacuum-Tube Oscillators to Inductive and Dielectric Heating in Industry

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I. Introduction

THE knowledge that metals could be heated inductively and that nonconductors could be heated dielectrically dates from the earliest experiments with electricity. In the last few years inductive and dielectric losses, which have long been obstacles in the electrical industry, have been profitably employed in a new industrial tool powered by high-frequency motor-generator sets and vacuum-tube oscillators. Low-frequency inductive heating has been used in specialized melting applications for approximately 20 years and for some surface hardening work for the last ten years. However with the advent of the new equipment and knowledge of its use, it is no longer a highly restricted application but is ready to handle thousands of heating processes throughout the industry. Hence, it is felt that a short review of the theory of inductive and dielectric heating, together with a description of the vacuum-tube circuits used and some of their applications, would be of value at this time.

II. Classification of Equipment

Inductive heating can be roughly segregated by frequency into three classes. The first class covers applications to 1,000 cycles. It is almost entirely used for forging and melting steel and nonferrous metals either directly or indirectly (that is, by heating electrically conductive crucibles). Power requirements range from a few watts to thousands of kilowatts generated by motor-generator sets. It is being widely used to produce many of the specialized alloys demanded for war equipment.

The second class covers frequencies from 1,000 to 12,000 cycles almost entirely generated by rotating machines with power ratings of from 20 to 1,200 kw. The applications in this group cover practically all heating processes—surface hardening, forging, brazing, soldering, melting are all included. One to three thousand cycles are used for melting nonferrous alloys, for surface-hardening relatively large parts, and for some forging applications. The new 9,600-cycle generators are being used successfully for surface-hardening relatively small parts where the contours need not be too closely followed, and where the depth of case need not be too small. Widely used for brazing and soldering of other high-resistance metals, inductive heating is more difficult to apply to the heating of copper, brass, and other low-resistance alloys.

The third group for inductive heating covers all applications from 50,000 cycles to 1,000,000 cycles. Generation is almost entirely restricted to spark-gap and vacuum-tube oscillators. Spark-gap type of equipment can be built for frequencies from 50,000 cycles to 200,000 cycles and for power outputs up to 25 kw. Vacuum-tube equipment, while having no theoretical frequency or power limitation, is most economically applied at frequencies over 150,000 cycles and at power outputs up to 200 kw.

In the dielectric heating field the vacuum-tube oscillator is the only source capable of producing sufficient power at the frequencies necessary. From 1,000,000 to 30,000,000 cycles have been used at powers up to 200 kw.

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III. Theories Involved

Before taking up some of the applications in greater detail, it might be well to review some of the fundamental theories involved. Any charge to be heated inductively can be thought of as a short-circuited secondary winding of a transformer, the primary being the heater coil. The currents produced in the charge heat it by resistive and hysteresis losses according to the well-established theory of eddy currents. The heat generated is confined to the surface of the parts by the skin-effect phenomena. The depth of penetration varies inversely with the square root of frequency, directly with the specific resistance of the material, and, in the case of magnetic materials, inversely with the permeability.

With nonmagnetic metals the heat is generated in this superficial volume throughout the heating cycle, varying only as the resistance of the metal changes

Figure 1. Five-kilowatt power oscillator for high-frequency induction heating

system voltage, and in changing through the inductance it will overshoot this amount and become a negative maximum of

$$\left(\frac{2C_2}{C_1+C_2}+1\right)E=\frac{C_1+3C_2}{C_1+C_2}E$$

which agrees with equation 13.

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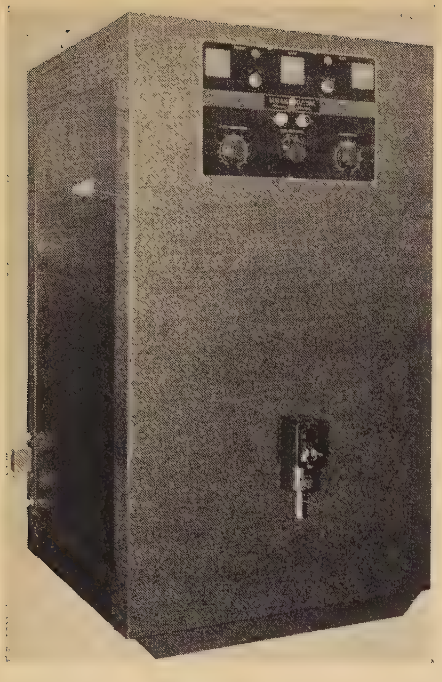


Figure 2. Fifteen-kilowatt power oscillator for high-frequency induction heating

with temperature—the core of the charge being heated largely by thermal conduction. Thus silver and copper are difficult to heat because of their low specific resistance and the resulting small depth of penetration. In magnetic materials the depth of the heated layer, which is very small due to the effect of the high permeability, increases rather abruptly at the temperature above which the material becomes nonmagnetic. Simultaneously, the power being absorbed in the original layer is reduced. Therefore steel with a high permeability and high specific resistance is readily heated in a thin surface layer while below the critical temperature, whereas above this temperature the power absorbed is less, and the depth of penetration considerably greater.

Because of these effects, the majority of steel parts can be readily surface-hardened at the intermediate frequencies by heating the surface inductively and quenching when the desired penetration has been achieved. Much higher frequencies are required for irregular parts where the contour must be followed closely, where coupling is difficult because of shape or small size, and where extreme localization of the heat is a necessity. To heat readily the low-resistance metals such as silver, copper, aluminum, and brass, the magnetic flux densities required at the intermediate frequencies are difficult to obtain, whereas the higher frequencies allow the use of more normal flux densities.

In dielectric heating the material to be heated is placed between two electrodes, and voltage applied at a frequency above 1,000,000 cycles. The whole forms a high-loss capacitor. The theory of dielectric losses is somewhat complex and is not yet thoroughly understood. However for most materials the heat developed is roughly proportional to the frequency and voltage applied and to the power factor of the dielectric. The entire mass is uniformly heated throughout, provided the electric field is uniform.

IV. Vacuum-Tube Circuits

The vacuum-tube oscillators used in the high-frequency field are of the simplest possible type. Radically departing from the usual radio construction practice, they are truly an industrial tool. Mechanically they are ruggedly built to withstand normal factory usage. Electrically they have been greatly simplified to provide the maximum reliability. The circuits most generally used are the Colpitts and the coupled-grid self-excited oscillator. In either of these circuits, the alternating supply voltage is stepped up by means of a power transformer to a voltage in the neighborhood of 7,500 volts to 15,000 volts and then rectified by a suitable bank of mercury vapor rectifier tubes. The direct current thus obtained supplies the oscillating circuit which consists primarily of a grid-controlled vacuum tube shunted across a parallel resonant circuit. The tube acts as a very rapidly operating switch. When this switch operates at the fundamental frequency of the parallel resonant circuit, it transmits power surges at this frequency by effectively short-circuiting the line once each cycle. Thus the voltage across the oscillation capacitor and inductance is alternately reduced and increased, setting up a circulating current in the resonant circuit.

To assure that the tubes operate at the desired frequency, their control grids are excited directly from the resonant circuit. This excitation voltage is obtained by several methods. In the Colpitts circuit (Figure 3) the excitation voltage is obtained by a direct tap on the oscillation capacitor, while in the coupled-grid circuit (Figure 4) it is developed in a coil inductively coupled to part of the oscillation inductance. All direct current is prevented from appearing in the resonant circuit by means of blocking capacitors. In both of these circuits the heater coil is usually in series with the parallel resonant circuit and forms part of the oscillation inductance. Since the frequency of operation is primarily determined by the constants of the resonant circuit, it will shift slightly as the inductance of the heater coil changes with load, thus always assuring operation at resonance and reducing fluctuations in the output current. For some applications requiring higher coil currents than are economically obtainable with a resonant circuit, an air-core transformer is used to step up the current.

V. Applications—Inductive Heating

This type of equipment can be applied to a great variety of heating problems. In the case of a small one-quarter-inch shaft the central portion was used as a bearing surface while the ends were riveted over to hold it in position. The overall length was approximately $1^{1}/_{4}$ inches. The problem was to harden the bearing surface while leaving the ends unaffected. Previously, this was achieved by copper-plating the ends and then casehardening the exposed parts by carburizing—a lengthy and expensive process. To apply induction hardening, a small coil was wound which was approximately the length of the desired hardened area. The part was centered in this coil, and the whole was immersed in water. Power at a frequency of 500,000 cycles was applied. As the steel heated up, a film of steam formed around that area, protecting it from the cooling action of the water.

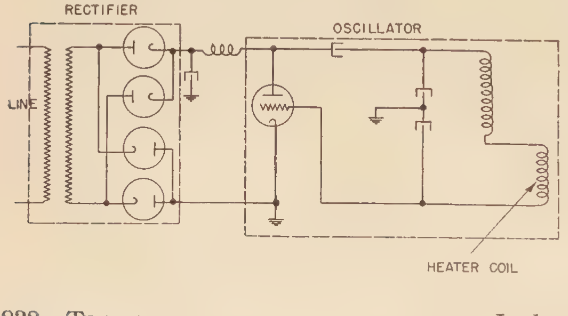
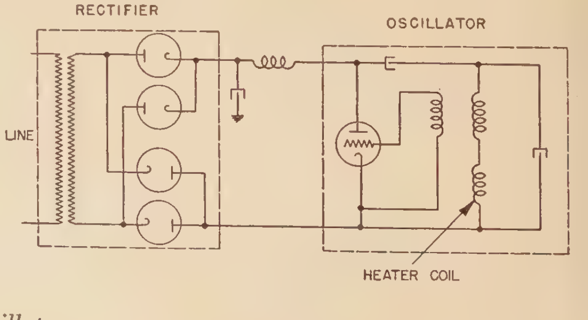


Figure 3 (left). Basic Colpitts oscillator circuit as used for induction heating

Figure 4 (right). Basic coupled-grid oscillator circuit as used for induction heating



But, when power was removed, this vapor envelope collapsed, allowing the water to rapidly quench the part. The entire cycle was complete in $3^1/_2$ seconds, and a satisfactorily hardened area obtained. Approximately four kilowatts output was used.

Curiously enough, in surface-hardening split pipe-threading dies an external coil can be used to harden the inside teeth. This is possible, because the induced currents must travel in a closed loop. Since there is no continuous external path around the circumference of a split die, the currents must travel completely around the inside of the die before returning to the outside surface. Thus such a die can be located inside a coil and, with proper heating and quenching cycles, can be hardened on both the inside and outside surfaces simultaneously, leaving a soft tough core.

In applying inductive heating to brazing, the usual procedure consists of assembling the fluxed parts using a preformed piece of brazing alloy between the parts. The advantages of the use of this method are speed, cleanliness, simplicity, and localization of heat. No experienced operators are necessary, and the method is admirably suited to automatic or semiautomatic operation.

In assembling the stator punchings of a small motor, the shell holding them had previously been spun over on either end. However, in using this method, the punchings had a tendency to work loose. To solve the problem, one end of the shell was spun over, the stator punchings assembled in position, and a thick washer placed over the end. A groove in the washer accommodated a ring of silver solder adjacent to the shell. The use of torch heating was impractical, because the heat conducted to the punchings tended to carbonize the insulating varnish. However, by using high-frequency induction, the heat could be so localized and the time interval used so short, that little heating of the punchings took place. Also, since the entire brazing operation could be performed while in a press, no loosening was encountered.

The manufacture of a small terminal

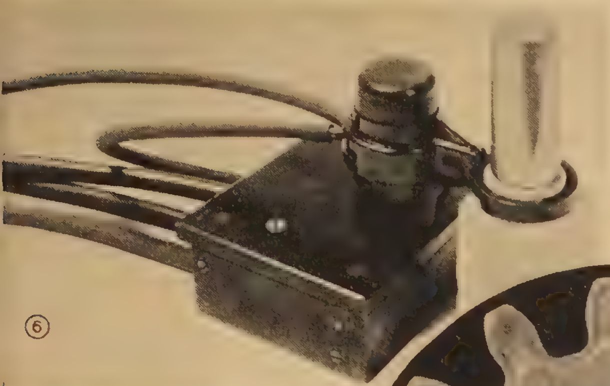


Figure 6. Brazing adapter on small assembly using air-core output transformer

Photo courtesy of Ajax Electrothermic Corporation

Figure 5 (right). Surface-hardened gear

Photo courtesy of The Ohio Crankshaft Company

Figure 8. Nickel-silver-sheath Calrod unit resistor with Mycalex terminals and bushings inductively brazed

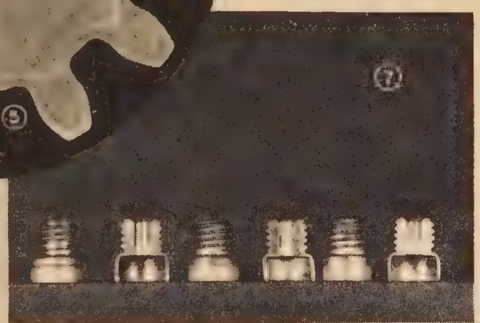


Figure 7. Terminal bushing assembly in which brazed joint and fused glass on rim were heated simultaneously

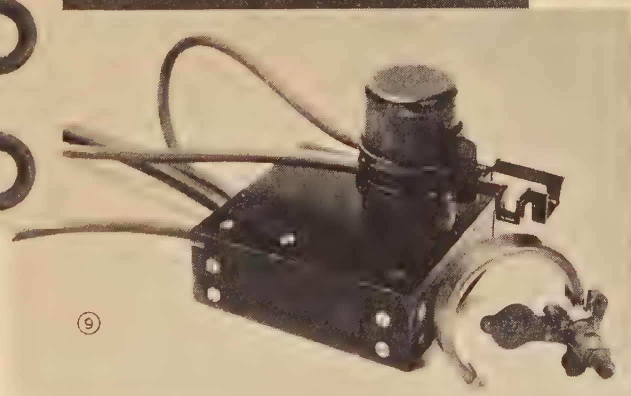


Figure 9. Focus inductor for specialized brazing showing output transformer with two-turn secondary winding

Photo courtesy of Ajax Electrothermic Corporation

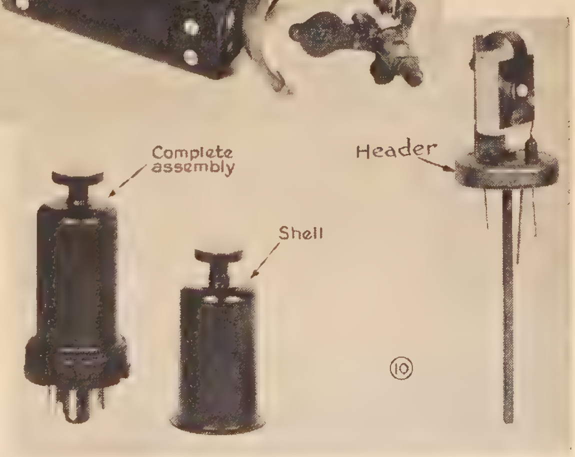


Figure 10. Metal-tube-type crystal unit

Shell soldered to header assembly by inductive heating

to be sealed in a glass bushing required two heating operations. First, the copper stud had to be silver-brazed to a nickel-steel alloy cup using a gas torch. Then, after spraying the rim of the cup with a suspension of finely divided glass in alcohol, the whole was fired in a muffle furnace to fuse the glass preparatory to sealing it into the insulator. These two operations are performed simultaneously, and a much more uniform product achieved by the induction heating method. The coil used is so proportioned that, as the proper temperature is reached for brazing at the joint, the flange is at the fusing temperature of the glass. The entire operation is complete in five seconds.

In the assembly of Calrod heating units the bushings and terminals are brazed in place. A great variety of metals must be heated since nickel-silver, copper, brass, and steel are all used for various applications. Also, the Mycalex insulating pieces must not be overheated or damage will result. Although the steel parts could be successfully heated using intermediate frequencies generated by rotating machines, high-frequency vacuum-tube equipment is used to obtain flexibility of operation throughout the range of materials used. Preformed silver solder rings are used. All operations being automatically controlled, there is no possibility of overheating or burning of the pieces. A further application of this machine has been the reclamation of short pieces of tubing previously regarded as scrap, since efforts to braze them together had been expensive and in many cases resulted in a weak joint. However, because of the uniformity of the brazed joint and the automatic operation made possible by inductive heat, these pieces are now being used successfully.

Because soft soldering is a low-temperature operation amenable to the use of resistance heating, only high-production items of an unusual nature have previously been attempted by induction methods. However, with the present-day enforced use of high-temperature soft solders, this application has been expanding rapidly.

In the manufacture of crystals as used in radio apparatus, one form of the crystal assembly is mounted on a header and enclosed in a shell very similar to the familiar small metal radio tube. The shell is soft-soldered to the base or header. This operation was performed in the past by pretinning the flange on the shell and the rim of the base and making the final joint in a ring burner, adding such solder as was necessary. However, the products of combustion of the gas used sometimes condensed on the crystal surface and seriously affected its operation. Also, the heat transmitted through the supporting bracket occasionally cracked the crystal. By heating inductively, the heat is entirely confined to the rim, and the operation considerably speeded up.

VI. Applications—Dielectric Heating

Dielectric heating of nonconducting materials is the newest application in the field and shows great promise of rapid development. Already one large unit has been placed in operation curing the glue in plywood with promise of many other such installations.* Several unsuccessful trials have been made to cure rubber by this method, but the applications to plastics, ceramics, and insulating compounds are progressing rapidly.

Not only will this type of heat reduce the time of curing of many plastic parts (a reduction in the case of thick laminated plastic insulating compounds of from hours to minutes), but it will allow the use of some types of plastics in forms previously impossible because of the difficulty of properly curing the material. Within the next two years it is expected that considerable progress will be made in this direction.

VII. Closure

A word of caution regarding the application of this type of equipment. The rotating machine and the high-frequency oscillator supplement but cannot replace each other. It is neither economically nor theoretically sound to attempt to heat large regularly shaped steel parts by use of oscillators, nor is it wise to use rotating machines of high power to heat a small irregular part where a low power oscillator could be applied. There is little overlapping of their respective fields of application, but, because of the great number of factors which must be considered, no broad definitions of their fields can be made. Thus each application must be judged on its own merit.

* Installed at M & M Woodworking Company, Plylock division, Albany, Oregon by the Thermex division of the Girdler Corporation.

TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Any discussion of these papers will appear in the December 1942 Supplement to Electrical Engineering—Transactions Section

Energy Flow in Electric Systems—the *Vi* Energy-Flow Postulate

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FELLOW AIEE

Synopsis: The conditions which a valid postulated electric-energy flow must satisfy are given and are stated to be insufficient for its unique determination. The commonly used *Vi* energy-flow postulate is shown by examples to be not generally valid, but by adding a simple term it can be made equally valid with other valid energy-flow postulates. Various examples are given of the application of this corrected energy-flow postulate. On power systems the engineer commonly limits his use of the uncorrected *Vi* postulate to applications where the correcting term should have a negligible net effect. Various examples of such use are discussed.

Introduction

AMONG power electrical engineers the following postulate as to electric-energy flow is extensively used. Let V be the electric potential at any particular point, referred to some arbitrarily chosen point of zero potential. Let i be the current density at that point. V, of course, is a scalar quantity, having magnitude but no associated direction in space; i is a vector, having magnitude and also direction in space. The product Vi is a vector and is postulated to describe or represent the density of a flow of electric energy in watts per square centimeter, V being in volts and i in amperes per square centimeter. In particular, the point in question may be a point in the cross section of a linear conductor or cable. The postulated electric-energy-flow density may be integrated over the section of the conductor to give the total postulated energy flow along the conductor. If, as is usually the case, the potential V is constant over the section of the conductor, then the total postulated energy flow will be VI, where I, a vector, is the total current flowing over the section of the conductor. This will be the reading of an instantaneous wattmeter connected into and to the conductor at that point. The average in time of this quantity VI will be the reading of the usual wattmeter at that point.

What are the phenomena which can be actually observed which justify or make valid the energy-flow postulate, if it is valid? They are the following. At some points in space electric energy is being generated or created. By that is meant that energy of some other well-known, recognizable, and measurable form is disappearing, and at the same time electric manifestations are taking place there, such as the flow of currents, the appearance of electric fields, and so forth. Thus, in a generator chemical energy of a pile of coal is disappearing, or potential energy of water in a reservoir is lessening in amount. In a discharging battery chemical energy is disappearing. In a thermocouple heat energy at the hot junction is disappearing at a rate greater than the heat energy appearing at the cold junction.

At other points in space recognizable and measurable forms of energy are appearing or being created, with simultaneous electric manifestations. In an electric oven heat is appearing in measurable amount. In an electric motor, mechanical energy is appearing as, perhaps, in the increasing potential energy of a rising elevator. In an electrochemical plant the chemical energy stored in matter is being increased.

Also, at various points in space electric and magnetic fields make their appearance, and it is found necessary, if the law of conservation of energy is to remain true, to assign a stored electromagnetic energy to these fields. It is commonly said: "It takes work to produce these fields." Where, as in the core of a reactor, there is a magnetic field, it is customary to assign a stored energy per cubic centimeter of amount $(10^{-7}/8\pi)H\cdot B$ joules where H is the magnetic intensity, and B is the magnetic induction in gauss. Where, as in the dielectric of a capacitor, there is an electric field, it is customary to assign a stored energy per unit volume of $^1/_2\ E\cdot D$ where E is the electric intensity in volts per centimeter, and D is the electric induction in coulomb-centimeters. In empty space, these assigned stored energy densities are respectively $\frac{10^{-7}}{8\pi}H^2$, and $\frac{1.11\cdot 10^{-12}}{8\pi}E^2$ joules per cubic centimeter. If these energy densities are integrated over all space, they give correctly the total energy which must be regarded as stored in the electromagnetic field if the law of conservation of energy is to remain true.*

At the various points of space this stored electromagnetic energy will be appearing or disappearing accordingly as the electric and magnetic fields are increasing or decreasing in intensity.

The postulated energy flow may be said to be substantiated, made valid, justified, or established if it properly co-operates with or fits in with these observable phenomena which have just been described. Where electric energy is being generated, the postulated energy flow must show energy flowing away and in proper amount. In regions where electric energy is being consumed, the postulated energy flow must make energy approach in proper amounts. At places where electric and magnetic fields are changing, and with them the associated stored energy, the postulated energy flow must make approach or recede the proper amounts of energy.

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* It turns out that these energy densities are not the only ones which may be assigned to the various points of space, which will integrate to the correct total electromagnetic energy. See, for example, reference 1. Therefore, these stored energy densities do not have a unique validity. However, in this paper, only these assumed energy densities will be considered in their relation to postulated energy flow.

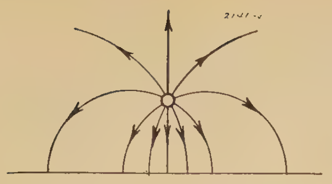


Figure 1 (left). Field E_1 in plane perpendicular to line

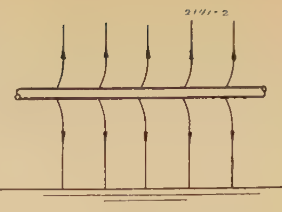


Figure 2 (right). Field E_1 in plane through line

The conditions establishing the validity of the postulated energy flow may be stated more exactly in this way. Construct a closed geometric surface anywhere in the electric system. Determine the postulated energy-flow density at every point of the surface. Integrate over the whole surface, obtaining the postulated total energy flow outward through the surface. This total energy flow out must correspond to the total changes taking place within the region enclosed by the surface. That is, the total energy flow out through the surface calculated according to the postulate must equal the excess of the rate of generation of electric energy over the sum of the rate of consumption and rate of increase of storage of electric energy within the surface.

It will be evident at once to the trained mathematician, and after a little study to the more lay engineer, that the conditions just given are not sufficient for determining uniquely an energy flow. For the postulate must merely direct the energy from the various generation points to the various consumption points, in their totality; which generation point is to feed which consumption point, and by what route, is completely undetermined so far as concerns any phenomenon which can be actually physically observed. Infinitely many postulates may be devised which will all be equally valid and equally well established by the conditions which have been described, and which are the only conditions available for defining an energy flow.

Actually, a postulated energy flow other than the *Vi* postulate, which is the subject of this paper, is very widely used, and not merely in abstract theory but for practical calculations, by radio engineers and physicists. This is the Poynting vector postulate about which there is an extensive literature.† We will not stop for a description of this Poynting vector postulate in detail at this stage. Suffice it to say that it is in its details completely different from the *Vi* postulate. In general, in an electric system it will lead energy from any particular generation point to a consumption point other than that to which it will be led by the *Vi* postulate, and where it leads energy from the same generation point to the same consumption point, it will in general do so by a different route.

Calling attention to the lack of uniqueness of the validity of any and all valid energy-flow postulates is not however the primary purpose of this paper. It is rather to examine the *Vi* postulate and to determine whether it in itself is valid or established in the only physically verifiable sense which has been just described. The completely general validity of the Poynting vector postulate has been established (with the formulas for stored electromagnetic energy given above) by mathematical derivation from Maxwell's equations. The validity of the *Vi* postulate can then be determined most conveniently by checking by well-known mathematical operations whether it is equivalent to the Poynting vector postulate in the sense already explained. This has been done in another paper.[1]

It turns out that the *Vi* postulate is not generally valid, that is, it is not generally equivalent to the Poynting vector postulate. By adding a simple term, however, a new or enlarged *Vi* postulate may be given which is generally valid. It so happens that as usually used by electric-power engineers the contribution of this added term is negligibly small. Hence, no error is committed by the use of wattmeter readings in the usual way for the usual and proper purposes by the engineers of electric-power systems.

Without going into the detailed mathematics, this paper will show how the simple *Vi* postulate fails in many simple cases. It will describe the term which must be added to make the enlarged *Vi* postulate universally valid and will show in simple cases how this term makes complete the *Vi* energy-flow picture. It will show also that this term makes a negligible contribution in the usual use of wattmeters on power systems for usual proper purposes. To do these things, however, it is necessary to examine somewhat carefully the meaning of the electric potential V, the current density i, and the nature of the electric field.

II. The Electric Potential, V

The potential difference between two points is frequently defined as the integral of the electric force from the one point to the other, or, in other words, as the work which must be done in moving a unit positive charge from the one point to the other. This is equivalent to saying that it is the reading of a suitable voltmeter connected to the two points in question.

For d-c systems, where electric and magnetic fields are steady and unchanging, this definition is satisfactory. In this case the electric-field intensity is the negative gradient of the potential so defined. In this case also the simple *Vi* postulate is generally valid.

In this d-c case, the electric field and the potential of which it is the negative gradient can be calculated from the distribution of charges. If there are no dielectrics, the potential V is given by

$$V = \int\int\int \frac{\zeta}{r} dv \qquad (1)$$

where ζ is the charge density at any variable point, r is the distance from this variable point to the point at which the potential is being determined, and the integration is carried out through all space. If dielectrics are present, then a term must be added under the integral of equation 1, giving the effect of the polarization density in the dielectrics, but for simplicity we shall leave this out of the formula, as it can be readily supplied by those readers sufficiently trained to feel the lack of it. The electric field is then given by

$$E = -\text{grad } V \qquad (2)$$

In the case of an a-c system, however, the definition of the potential given at the beginning of this section fails. The work done in moving a unit charge from one point to another is no longer uniquely determined. The work done is different according to the path chosen in going from the one point to the other. For example, the work done in moving a charge from a point near the core of a transformer to a point diametrically opposite, by a path half way round the core,

Figure 3 (left). Magnetic field around line

Figure 4 (right). Induced electric field E_2

† For description of energy flow in usual electric-power machines, according to the Poynting vector postulate, see Slepian, *Electric Journal*, volume 16, July 1919, page 303.

will be different from the work done if the charge is moved from the one point to the other in the other direction round the core. If a suitable voltmeter is connected between the two points, the reading will be different according to whether the leads go round the core by the one path or the other.

We may however define a function calculated from the observable distribution of charges (and polarizations of dielectrics) by means of equation 1. We shall call this the Maxwell scalar potential,‡ V_M, so that

$$V_M = \int\int\int \frac{\zeta}{r} dv \tag{3}$$

Of course, the electric field is not the negative gradient of this scalar potential

$$E \neq -\text{grad } V_M \tag{4}$$

In fact E is not the gradient of any potential.

We may regard the electric field E as made up of a superposition of two fields, E_1 and E_2,

$$E = E_1 + E_2 \tag{5}$$

where

$$E_1 = -\text{grad } V_M \tag{6}$$

E_1 may be said to be the field produced electrostatically by the charges defining V_M.

If a charge is moved around in a closed path, the work done against the component field E_1 is necessarily zero, since E_1 is the gradient of a potential. If there is a net work, not zero, done by the charge in going round the closed path in the total field E, it must be the work done against the component field E_2. Thus the integral of E_2 around a closed path is in general not zero. By Faraday's law, this integral, sometimes called the induced electromotive force, is equal to $1/10^8$ times the rate of change of the enclosed magnetic flux, the units of electric field and magnetic flux being the usual volts per centimeter and maxwells.

The lines of force of the field E_2 cannot terminate on any charge, since equations 3 and 6 make E_1 bring to or from all the charges all the corresponding lines of force, thus leaving no charges for the lines of E_2 to terminate upon. The lines of force of E_2 then must form closed loops, linking varying magnetic flux. E_2 may then be said to be the electric field induced by the varying magnetic field, and to have no electrostatic character.

‡ Physicists now more generally use the retarded scalar potential of Lorentz. Except for very long lines in power systems, or high frequencies, this potential will differ very slightly from the scalar potential of Maxwell, which is used here because of its simpler description.

Mathematically the defining properties of E_1 and E_2 are as follows:

$$\text{div } E_1 = -4\pi\zeta,\ \zeta \text{ in appropriate units} \tag{7}$$

$$\text{div } E_2 = 0 \tag{8}$$

$$\text{curl } E_1 = 0 \tag{9}$$

$$\text{curl } E_2 = -1/10^8 \frac{\partial B}{\partial t} \tag{10}$$

It appears that the potential used by electric-power engineers is precisely the Maxwell scalar potential which has just been described.

For example consider a single transmission line parallel to the surface of a good conducting earth, which is taken as having zero potential. The charges on the line by themselves, if they were unvarying, would produce the field E_1 with lines of force lying nearly completely in planes perpendicular to the line, as in Figures 1 and 2. The slight curvature in the lines of force of E_1, and their inclination to the line, shown in Figure 2 somewhat exaggerated, are due to the charge density on the line decreasing from left to right, due to the decreasing "potential" of the line.

The current in the line, produces the magnetic field shown in Figure 3, the lines of magnetic force lying in planes perpendicular to the line. If the current is increasing, the magnetic field also increases and induces an electric field E_2 whose lines of force are nearly parallel to the line as in Figure 4. This field is strongest at the line, where it is nearly equal, and opposite in direction to the component of the field E_1 which is parallel to the line. The parallel component of the net field, E, at the line is that required by the ohmic resistance of the line to the current it is carrying. The total field E is the sum of the electrostatic field E_1 and the induced field E_2.

To determine the potential at a point 1 in the line, the power engineer connects a voltmeter from that point to the ground immediately below. That is, he integrates the electric force along a path in the plane perpendicular to the line. Since such a path is perpendicular to E_2, he essentially integrates the field E_1 alone and, therefore, by equation 6, determines the Maxwell scalar potential, V_M, at the point 1 of the line.

To determine the potential at a second point 2 in the line, the engineer connects his voltmeter from the point 2 to the earth immediately below, and not to the earth point used for getting the potential at point 1. To find the potential difference between points 1 and 2, the engineer takes the difference between the potentials so determined. He does not connect a voltmeter from point 1 to point 2 with leads running parallel to and along the line. If he did, the field E_2 would contribute to the integral of E from 1 to 2 and he would get a result differing from that obtained by his usual procedure. He would say that this last procedure would give him only the resistance drop in the line, whereas his usual procedure gives him the total (resistance plus inductance) drop in the line. It is clear then that the power engineer uses the Maxwell scalar potential. Therefore, subsequently in this paper, the subscript M will be dropped from the V symbol for potential, it being understood that the Maxwell scalar potential is that which is meant.

III. The Corrected Vi Postulate

The appearance of the induced field E_2 is one of the features which distinguishes the variable-state or a-c system from the steady-state or d-c system. If E_2 is absent or negligibly small, then the usual energy-flow postulate,

$$P_1 = Vi \tag{11}$$

is generally valid, provided i includes displacement currents as well as conduction currents, as will be explained in the next section. If however, E_2 is not absent, equation 11 becomes invalid. Then the following energy-flow postulate,

$$P_2 = Vi + \frac{10}{4\pi}[E_2 \times H] \tag{12}$$

is generally valid. The bracket symbol in equation 12 indicates the vector product of E_2 and H, whose meaning will be explained later in examples. The two terms in P_2 may be called respectively the conductive and inductive components of the postulated energy flow.

IV. The Current Density i—Example of Variable-State System Where P_1 Is Valid

To see how the inclusion of displacement currents in i makes P_1 valid as long as E_2 is absent, consider the following example:

A sphere of radius R, far removed from other objects, is charged to potential V_o. It is then surrounded by a spherically symmetrical radial electric field, with $V = V_o R \frac{1}{r}$, and $E = V_o R \frac{1}{r^2}$ where r is the distance from the center. There will then be a stored energy density, $T_e = \frac{1.11 \cdot 10^{-12}}{8\pi} E^2 = \frac{1.11 \cdot 10^{-12}}{8\pi} V_o^2 R^2 \frac{1}{r^4}$ joules per cubic

centimeter in the space around the sphere.

Now let the sphere be joined by a fine high-resistance wire to the remote ground. The sphere slowly discharges by the conduction current through the wire. The small current flowing produces a magnetic field, but let us suppose it is so small that we may neglect the stored energy which is associated with it and also the small induced field E_2 which it produces by its slow change.

The stored energy in the electrostatic field slowly disappears, and a corresponding amount of heat appears in the wire. The valid energy-flow postulate, $P_1 = Vi$ must show energy flowing from the space where it is initially stored to the wire where it appears as heat.

In the wire itself, let the total conduction current be I. At a point where the potential is V_a, the postulate P_1 asserts that there is an energy flow in the wire equal to V_aI watts away from the sphere. At a more remote point on the wire, where the potential is V_b, the energy flow in the wire is V_bI, watts away from the sphere. V_bI is less than V_aI, the difference being $(V_a - V_b)I$. But if ζ_{ab} is the resistance of the wire in ohms between points a and b, $V_a - V_b = \zeta_{ab}I$, and $(V_a - V_b)I = \zeta_{ab}I^2$ which is precisely the joulean heat developed in the wire between a and b. Thus the postulate $P_1 = Vi$ shows energy flowing from the sphere down the wire where it appears as heat at the proper places and in the proper amounts.

But the valid postulate $P_1 = Vi$ must also show how the energy stored in the electrostatic field arrives at the sphere so that it may be directed thence into the wire. The displacement current density comes to the rescue for this purpose.

Where an electric field E is varying, in free space, a displacement current density is defined as $i = \frac{1.11\cdot 10^{-12}}{4\pi}\frac{\partial E}{\partial t}$ amperes per square centimeter. In the present example E is decreasing so that there is a displacement current density flowing inwards towards the sphere given by $i = \frac{1.11\cdot 10^{-12}}{4\pi}R\frac{dV_o}{dt}\frac{1}{r^2}$. The total displacement current flowing inwards over the surface of a sphere of radius r_a will be $4\pi r_a^2 i = 1.11\cdot 10^{-12}R\frac{dV_o}{dt}$. The potential at r_a being $RV_o\frac{1}{r_a}$ volts, there is a total energy flow inwards across the surface, according to the *Vi* postulate, of $1.11\cdot 10^{-12}R^2V_o\frac{dV_o}{dt}\frac{dV_o}{dt}\frac{1}{r_a}$ watts. Across a more remote sphere, of radius r_b, the total energy flow inwards is $1.11\cdot 10^{-12}R^2V_o \times \frac{dV_o}{dt}\frac{1}{r_b}$. The flow inwards across the inner sphere exceeds the flow inwards across the outer sphere by $1.1\cdot 10^{-12}R^2V_o\frac{dV_o}{dt} \times \left(\frac{1}{r_a} - \frac{1}{r_b}\right)$ watts. This should be equal to the rate of decrease of the energy stored in the shell between the two spherical surfaces.

But integrating the stored energy density, $T_e = 1.11\cdot 10^{-12}V_o^2R^2\frac{1}{r^4}$ over this spherical shell, we get $\frac{1.11\cdot 10^{-3}}{2}V_o^2R^2 \times \left(\frac{1}{r_a} - \frac{1}{r_b}\right)$ joules. The rate of change of this energy, $1.11\cdot 10^{-12}V_o\frac{dV_o}{dt}R^2\left(\frac{1}{r_a} - \frac{1}{r_b}\right)$ checks exactly with the assertion of the *Vi* postulate in the preceding paragraph.

Figure 5 shows diagrammatically this flow of the stored energy to the sphere and thence to the wire according to the *Vi* postulate. For comparison, Figure 6 shows the energy flow according to the Poynting vector postulate.

V. Example of Failure of Simple Vi Postulate

Consider a long straight linear conductor of circular section and radius R. Let a current flow in it which is increasing at a constant rate, $I = I_0t$. Let the return circuit be the parallel-plane good conducting ground a considerable distance below the conductor.

There will be a magnetic field surrounding the conductor as in Figure 3, and this field will be increasing in strength at a constant rate. The stored energy associated with this field and distributed in the space will also be increasing. A valid energy-flow postulate must show a flow of energy into these regions where the stored energy density is increasing.

A potential drop (Maxwell scalar potential drop) will exist along the line, called by the engineer the inductive drop along the line. Neglecting the ohmic resistance of the line this potential drop will be constant in time. Associated with this distribution of potential on the line will be an electrostatic field E_1 as pictured in Figure 2. This field will be constant in time.

The steadily increasing magnetic field of Figure 3 will induce an electric field E_2 as pictured in Figure 4. This induced field E_2 is also constant in time.

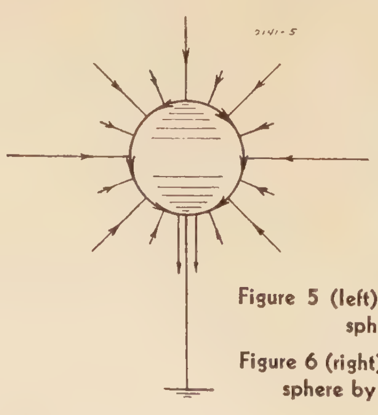
Let us apply the simple $P_1 = Vi$ postulate to this case. It shows an energy flow in the line at each point of an amount VI watts, where V is the potential at the point. Everywhere else, other than in the line, the current density is zero. There is no displacement current in the space because the electric fields E_1 and E_2 are constant in time. The simple $P_1 = Vi$ postulate then shows energy flowing in the line and nowhere else.

Consider a short length of the line. Because the potential at the entering end is larger, the energy flow entering the section is greater than that leaving the section. Hence, if the simple *Vi* postulate is valid, there should be appearing in the section an increasing amount of some form of energy, such as heat for example. But no such energy appears there! The simple *Vi* postulate fails!

Likewise, out in space the stored magnetic energy is increasing. The *Vi* postulate should show an energy flow bringing this stored energy to its proper position in space. It does not! It fails!

VI. Success of the Enlarged Postulate, $P_2 = Vi + \frac{10}{4\pi}[E_2 \times H]$, With Preceding Example

The bracket in the second or inductive term of the enlarged postulate, P_2, denotes the vector product of the induced electric field E_2 and the magnetic field H. This is a vector which stands perpendicular to both E_2 and H, with direction given by the right hand rule, that is, the direction of advance of a right-hand screw when



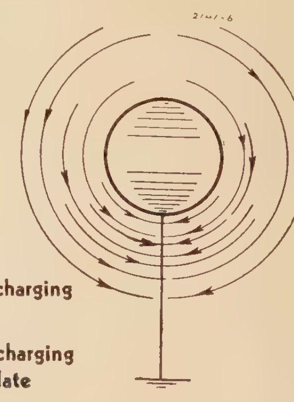


Figure 5 (left). Energy flow for discharging sphere by *Vi* postulate

Figure 6 (right). Energy flow for discharging sphere by Poynting vector postulate

turned in the sense which brings E_2 most quickly into line with H. Its magnitude is equal to the product of the magnitudes of E_2 and H, respectively, and the sine of the angle between them.

Let the induced electric field E_2 have the value E_{20} at the conductor itself. The (Maxwell scalar) potential gradient along the conductor will have the same magnitude as E_{20}, since we are neglecting the ohmic resistance of the line, but will be oppositely directed.

Let the length of the portion of the line we are considering be l centimeters. The potential drop through this portion will then be $l|E_{20}|$, $|E_{20}|$ being the magnitude of the vector E_{20}. The first or conductive term of P_2 then will give an excess of energy entering the one end of the line

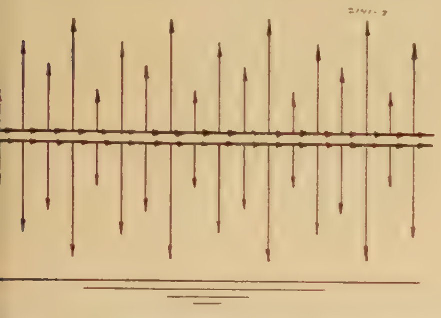


Figure 7. Energy flows about line carrying increasing current by P_2 postulate

portion over that leaving the other end by an amount equal to $l|E_{20}|I$ watts.

At the surface of the line, the magnetic field H_o is perpendicular to the line and E_{20} and has the magnitude $|H|_o=\frac{2I}{10R}$. The second term of P_2 then shows an energy-flow density leaving the conductor surface of $\frac{10}{4\pi}|E_{20}|\frac{2I}{10R}=\frac{1}{2\pi}|E_{20}|\frac{I}{R}$ watts per square centimeter. Multiplying by the area of the surface of the line, $2\pi Rl$, we find the second term showing an energy flow out of the line of an amount $l|E_{20}|I$ watts. Comparing with the preceding paragraph we see that the enlarged postulate P_2 calls for the appearance in the line of heat or other energy of amount zero! In the line P_2 succeeds!

In the space outside the line, while the first term of P_2 shows zero energy flow, the second or inductive term shows energy flow radially outwards into space. It is not difficult to show that this radial flow is of just the proper amount to account for the increasing magnetic energy stored there. Figure 7 shows diagrammatically the energy flow about the line according to P_2.

P_2 succeeds everywhere! It must, since it has been shown in the paper of footnote 1 that it is equally valid with the Poynting vector postulate.

VII. Energy Flow in Transformer

It is instructive to trace the flow of energy in a transformer according to the simple and enlarged Vi postulates. The simple Vi postulate fails, of course. The enlarged postulate succeeds because of its inductive term.

In each turn of the primary of the transformer, there is a potential drop equal to the induced voltage per turn, V_t, if the resistance drop is neglected. According to the simple Vi postulate then, more energy flows into the turn than out by an amount V_tI_p watts, where I_p is the primary current. The simple Vi postulate then makes $V_tn_pI_p$ (n_p=number of primary turns) watts disappear in the primary, apparently causing energy to be annihilated.

Similarly, the simple Vi postulate calls for the apparent creation of energy in the secondary at the rate $V_tn_sI_s$ watts.

The second term of the enlarged Vi postulate avoids this apparent distressing destruction of energy in primary and creation of energy in secondary by providing an energy flow from primary to secondary.

Between the primary and secondary coils there will be a magnetic field perpendicular to the coil wires, Figure 8. This field will have magnitude approximately $|H|=\frac{4\pi}{10}\frac{n_pI_p}{l_1}$ where l_1 is the length of the coil. Between the coils there will also be an induced electric field E_2 with lines of force remaining parallel to the coil wires and perpendicular to H. The magnitude of E_2 will be approximately $|E_2|=\frac{V_t}{l_2}$ where l_2 is the perimeter of a turn.

According to the second term of P_2, then, there is an energy flow from primary to secondary of $\frac{10}{4\pi}[E_2\times H]=\frac{V_tn_pI_p}{l_1l_2}$ watts per square centimeter. Multiplying by l_1l_2, the total area carrying this energy-flow density, we get $V_tn_pI_p$ watts carried across from primary to secondary by the second term of P_2, which is just the energy brought into the primary by the first term Vi.

VIII. Relationship Between Poynting Vector Postulate and P_2

The Poynting vector postulate asserts the presence of an electric-energy-flow density given by

$$P=\frac{10}{4\pi}[E\times H] \text{ watts per cm}^2 \tag{13}$$

It differs from the enlarged Vi postulate, P_2 in making all the energy flow inductive, and none conductive.

This suggests that the resolution of the energy flow into a conductive and inductive component as in P_2 is largely arbitrary. This is true.

Let V' be any *arbitrary* scalar function of position in space. Let E_1' be a vector function of space defined by

$$E_1'=-\text{grad } V' \tag{14}$$

Let E_2' be defined by

$$E=E_1'+E_2' \tag{15}$$

Then it is readily shown that the energy flow postulate

$$P_2'=V_t'+\frac{10}{4\pi}[E_2'\times H] \tag{16}$$

is equally valid with P and P_2.

This bears out the statement made in the introduction that infinitely many valid energy-flow postulates may be devised.

The Poynting vector P, and the corrected Vi postulate P_2, are special cases of equation 16, obtained by respectively taking $V'=0$, and taking for V' the electric potential V.

IX. Validity of Simple Vi Postulate for Usual Proper Purposes on Power System

The power-system engineer postulates an energy flow in his lines given by connected wattmeter readings and with this postulate carries on his usual technical and commercial operations. This postulate is identical with the simple Vi postulate (except for the omission of displacement currents) which we have seen is not generally valid. The postulate can be made valid by the addition of the inductive term of P_2. Hence, since the engineer is successful in his usual operations, we conclude that he limits himself to those applications where the contribution of the inductive term of P_2 is negligible.

We have also seen that even the valid enlarged Vi postulate, P_2, is not uniquely valid. Hence, again since the engineer is successful in his usual operations, we conclude that he normally limits himself to those questions for which all valid postulated energy flows will give the same

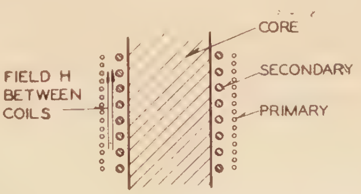


Figure 8. Magnetic field in transformer

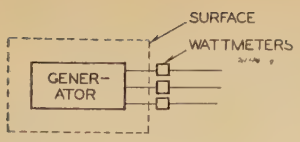


Figure 9 (left). Energy of generator by wattmeters

Correct connection

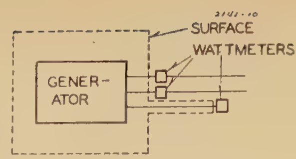


Figure 10. Energy of generators by wattmeters

Incorrect connection

answer. In referring back to the introduction, there is only one question to which all valid energy-flow postulates give the same answer, namely, the following: In a given region of space, in which there may be any and various kinds of electric apparatus, what is the excess of the total electric-energy generation over the sum of the total energy consumption and the increase in electric-energy storage?

Before giving examples, it is well to point out one use of wattmeter readings which is frequently made and which does not involve the energy-flow postulate at all. It is for determining the loading or heating of lines.

Now, the heating of a line certainly does not depend on any so intangible and intrinsically indeterminable a thing as a postulated energy flow. The heating is determined only and entirely by the current through it, and this is measured unequivocally by an ammeter. However, from the wattmeter reading the engineer, if he knows the usual voltage and power factor of the line, can make an estimate of what the current may be and thus estimate the loading or heating of his line. But in doing this the engineer is really using the wattmeter, together with his other knowledge of the line, as a kind of devious ammeter. Regardless of how he may colloquially refer to energy or power or the line, he knows that the heating of the line is determined by the current magnitude only.

As a first example, consider the determination of the energy generated by a generator. The engineer places wattmeters in all the leads coming from the generator, adds their readings algebraically, and asserts that number so obtained is the desired generation. He will be right if the inductive term of P_2 makes no contribution.

Surround the generator by a closed surface passing through the points in the leads where the engineer connected his wattmeters (Figure 9). The validity of P_2 ensures that, when integrated over this closed surface, it will give the net rate of energy generation in the generator since the energy stored remains constant. The conductive term, Vi, of P_2, will be zero everywhere in the surface except over the sections of the generator leads. This term integrated over the surface will equal the sums of the wattmeter readings.

E_2 and H which enter into the inductive term, $\frac{10}{4\pi}[E_2 \times H]$ of P_2, will each be negligibly small on the surface, except in the neighborhood of the leads. There, as explained in sections V and VI and shown in Figure 3, E_2 is parallel to the leads and therefore perpendicular to the surface. H however will lie parallel to the surface. Hence $\frac{10}{4\pi}[E_2 \times H]$ lies entirely parallel to the surface and makes zero contribution to the total energy crossing the surface by the P_2 postulate. Hence P_2 integrated over the surface equals the sum of the wattmeter readings, and the engineer's usual use of wattmeters for determining the energy generation of a generator is justified.

It is clear from this discussion that the validity of the use of wattmeters alone in the preceding example depends on the points of attachment of the wattmeters to the leads lying in a surface which is perpendicular to the E_2 field. The engineer is aware of this. If one of the wattmeters is displaced along one of the leads from its proper position, as in Figure 10, the engineer knows that the sum of their readings will no longer equal the rate of energy generation in the generator. Ordinarily he will say that this is due to the inductive action of the leads, each on the other. This paper describes this situation by observing that the inductive term of P_2 in this case asserts a flow of energy not zero over the portion of the enclosing surface, which lies parallel to the leads.

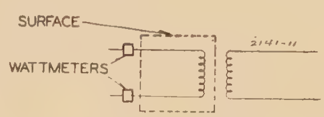


Figure 11 (left). Determination of loss in primary of a transformer

Another example: The engineer will not attempt to determine the losses in the primary coil of a transformer by means of wattmeters alone. In referring to Figure 11, showing a transformer diagrammatically, it is clear that for a surface which encloses the primary alone, there will necessarily be some portions across which the inductive term of P_2 will also assert there is an energy flow, as explained in section VII, and this energy flow must be added algebraically to the wattmeter readings to obtain the primary losses. However, the engineer will not hesitate to determine the total losses in a transformer alone, since in that case an enclosing surface may be drawn such that across it the inductive term of P_2 will assert zero energy flow.

A last example: Two power systems, A and B, operate with an interconnecting transmission line, in which wattmeters are properly placed. The engineer advises that the owners of these systems make payments to one another depending on the sum of the readings of these wattmeters. What is the physical basis on which the engineer makes his recommendation?

Referring to Figure 12, a surface may be drawn through the points of connection of the wattmeters and enclosing completely the system A, such that the contribution to the energy flow across the surface according to postulate P_2 of the inductive term is zero. The contribution of the conductive term will of course equal the sum of wattmeter readings. Hence we may conclude that the sum of the wattmeter readings equals the excess of the generation in system A over the sum of all the loads and losses in system A.

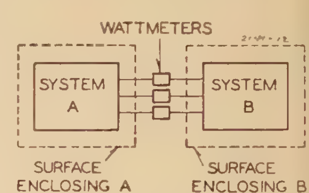


Figure 12. Power systems interconnected

Similarly, a surface may be drawn through the wattmeter connections and enclosing the system B, which will justify the statement that this same sum of the wattmeter readings also equals the excess of the sum of the loads and losses over generation in system B.

Because of this numerical relationship between the wattmeter readings and the balance between generation, loads, and losses on the two respective systems, the engineer makes his recommendation that the owners of system B compensate the owners of system A. The engineer may in addition make some irrelevant assertions concerning energy flow in the transmission line, while a physicist may equally irrelevantly speak of an energy flow from A to B through space.

The owners of the systems are indifferent to these reflections of engineer and physicist. Their esthetic tastes may perhaps be more like that of the engineer so that they may prefer an energy flow postulate P_2 which makes some of the energy (but not all) flow in wires, to one like P, where nearly all of the energy flows in space and quite remotely from the wires.

But what actually moves the owners of A to expect compensation is the physically observable fact that the generation in A exceeds the sum of the energies billed for at all the loads in A plus the losses in A. Likewise the owners of B are persuaded to make compensation by the physically observable fact that the total energy generated in B is less than the sum of the energies billed for at all the loads in B plus the losses in B. These same physically determinable facts as to location and magnitude of electric-energy generation and consumption, which basically are those alone in which owners of power systems are interested when engaged in "exchanges of energy," are also the only physically determinable facts on which the infinitely many valid energy-flow postulates will agree. The engineer is quite right in choosing one of them, namely $P_2 = Vi + \frac{10}{4\pi}[E_2 \times H]$, for determining the magnitude of the so-called "exchange of energy," and applying it under such a condition that the second term may be neglected, and determining the effect of the first term by wattmeter readings.

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Power-System Interconnection in Quebec

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MEMBER AIEE

I. Introduction

THE main purpose of this paper is to describe the development of a major interconnected system located in the province of Quebec.

By means of a study, based on prewar data when stable conditions existed, and by suitable illustrations and explanations, it is aimed to show how it has been possible, through interconnection of three important hydroelectric systems, to increase the combined firm power capacity far beyond the sum of the capacities of the individual systems if operated independently. It will be brought out that the diversity in load characteristics and hydraulic conditions makes it feasible to accomplish this result and to effect other desirable economies on these systems, which are dependent solely on power supply from hydroelectric plants.

The possibilities of system consolidation had been under study for some years, but the war, with the urgent demand for more firm power, forced the issue to a conclusion, involving combined power resources of nearly 4,000,000 horsepower as indicated in Table I.

Reference will also be made to certain engineering features and operating practices of these systems, which may prove to be of interest.

II. Description of Geographical Areas

In leading up to a detailed analysis of the present interconnected system, it is thought desirable to describe briefly the areas under consideration.

The Montreal Area. This area is considered as including the territory in the immediate vicinity of Montreal Island on which is located the city of Montreal. Power is supplied by the Montreal Light, Heat, and Power Consolidated principally from two power developments on the St. Lawrence River. The plants of the Montreal Island Power Company and of the Canadian Light and Power Company also feed into this area.

Originally, in the year 1900, electric service was supplied by a number of small isolated power companies, not interconnected in any way and each serving its own independent district. In most cases standby steam plants were necessary to guard against the frequent interruptions to the supply from the hydroelectric plants because of ice trouble, breakdown, and other causes.

In 1902 the Montreal Light, Heat, and Power Consolidated was formed and acquired jurisdiction over most of the isolated systems in and near Montreal. It was soon realized that for reasons of economy, and to secure better service, some consolidation of systems or interconnection between plants was essential.

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While some immediate constructive steps were taken in this direction, it was found that parallel operation was difficult or in some cases impossible because of the different frequencies, these being 66, 60, and 30 cycles. Furthermore, some systems were two-phase, while others were three-phase, and even direct-current. Eventually by compromising on 63 cycles, tying in through frequency changers, increasing the 30-cycle frequency to 31.5 cycles, and adopting three-phase alternating current, a common operating base was obtained, but this did not necessarily indicate parallel operation of the combined systems. Actually it was not until 1923 that all the subsystems of the Montreal area were operated completely in parallel.

The Shawinigan Area takes in the territory on both sides of the St. Lawrence River from Montreal to Murray Bay, some 80 miles below Quebec City, and is served mainly by the Shawinigan Water and Power Company, the Quebec Power Company, and the Southern Canada Power Company.

The Saguenay Area in the Lake St. John region is served mainly from plants on the Saguenay River by companies under the control of the Aluminum Company of Canada, Ltd.

III. Load, Output, and Hydraulic Characteristics of the Individual Systems

In the *Montreal Area* the St. Lawrence River is the chief source of power and has a flow which is unusually uniform, the average mean flow being 220,000 cubic feet per second, while the maximum and minimum recorded flows are 318,000 cubic feet per second and 173,000 cubic feet per second respectively. The heads involved are from 26 to 80 feet; comparatively long headrace canals are found, with the main difficulties in power output being caused by ice jams or other forms of ice trouble, occurring during the months of January and February. A typical hydrograph is shown in Figure 1.

The nature of the load in the Montreal area is that which might be expected in any large metropolitan area, with week-day load factor in the winter months of the order of 68 per cent and an annual load factor of 47.5 per cent during the prewar period. Maximum peak loads occur in the months of November or December, with a definite tapering off during the summer months. This is well

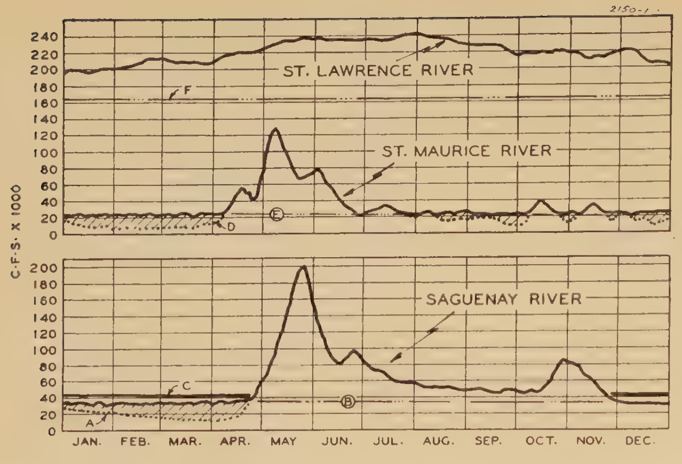

Figure 1. Hydrographs of St. Lawrence, St. Maurice, and Saguenay Rivers

A. Unregulated flow, Saguenay River

B. Regulated flow 35,000 cubic feet per second, 1942

C. Estimated regulated flow, 45,000 cubic feet per second 1943

D. Unregulated flow, St. Maurice River, 6,000 cubic feet per second, minimum

E. Regulated flow, 22,000 cubic feet per second, 1942

F. Utilization of St. Lawrence River flow, 1942—164,000 cubic feet per second

illustrated by comparing the typical daily load curves shown in Figure 2.

The chief need consequently is peak power for short periods during the winter months, while during the remainder of the year large and variable amounts of surplus energy and power are indicated, particularly during the months of April to November inclusive, with no immediate market available.

From the point of view of output, the gross primary kilowatt-hours sold in this entire area for 1941 were 3,341,902,000, while the corresponding maximum system peak load for primary power was 586,200 kw.

In March 1942, there was available 60-cycle capacity horsepower to the extent of 650,000 horsepower apart from that available from the Shawinigan Water and Power Company under the 160,000-horsepower contract.

In the Shawinigan Area the St. Maurice River is the most important source of power. It has a drainage area of 16,000 square miles and is suitably equipped for good regulation, having a number of storage reservoirs with a total storage of 13,000 square-mile-feet. This gives an indication of the extent to which the river is regulated and the possibilities of storing water for the combined system.

By using these storages, the regulated flow (Figure 1) can usually be maintained at 22,000 cubic feet per second, as compared with the unregulated minimum flow of 6,000 cubic feet per second. The maximum flow now experienced is of the order of 120,000 cubic feet per second which occurs early in May. As a result plants on this river have little difficulty in maintaining a high rate of output, excepting during the high tail-water period when in certain cases the plant output is markedly reduced. It so happens, however, that in the month of May the Montreal area is in a position to rectify this condition to a great extent, so that from the hydraulic standpoint, there is some diversity between the Montreal and Shawinigan areas.

From the load standpoint the Shawinigan system firm load is of an unusually high load factor, being of the order of 92 per cent on a week day, with an annual load factor under prewar conditions of 71 per cent. The type of firm load includes paper mills, aluminum production, carbide and carborundum furnaces, and asbestos mills, as well as a few steel plants.

Until recently, on account of the important paper-mill load which required the supply of quantities of process steam, large amounts (maximum peak over 500,000 horsepower) of surplus energy were supplied to electric boilers to replace coal-fired steam at the mills. This surplus was not only that obtainable from the Shawinigan area proper but also included secondary power purchased from neighboring companies.

Output characteristics of the Shawinigan system during the year 1941 included gross kilowatt-hour sales of 6,270,760,543 kilowatt-hours with a maximum 60-cycle firm peak of 868,000 kw. In March 1942 this area was served by 30-cycle and 60-cycle capacity to the amount of 1,154,500 horsepower aside from the 100,000-horsepower firm power contract with the Saguenay Power Company.

In the *Saguenay Area*, the Saguenay River is fed from a watershed area of 28,100 square miles. The hydrograph of this river (Figure 1) is somewhat similar to that of the St. Maurice River but is fortunately different as to the time of occurrence of the high water period, thus indicating advantages from interconnection with the St. Maurice River plants.

Prior to 1941 no storage reservoirs on this watershed had been constructed for regulating the flow of the Saguenay River, but Lake St. John itself (6,080 square-mile-feet usable storage) is utilized to the greatest practical extent from December to March inclusive.

Regarding load characteristics: before the war, power developed in the Saguenay area was utilized principally for the production of paper and aluminum and to supply the 100,000-horsepower firm power contract with the Shawinigan Water and Power Company. During periods of excess energy, the surplus was utilized for electric steam generation, the output for this purpose attaining at times the value of 375,000 horsepower. Since 1940, on account of the tremendous increase in demand for firm power, the electric boiler load has been discontinued.

The gross output of the plants in the Saguenay area during 1941 was 4,538,000,000 kilowatt-hours with a maximum firm peak of 639,000 kw. To serve this area, as in March 1942, there was capacity normally available to the extent of 1,000,000 horsepower.

IV. Possibilities of Maximum Utilization of Resources by Interconnection

In order to illustrate how the differences in the system characteristics of the three areas, could be utilized fully, a study has been made and a series of curves (Figure 3) prepared to show the peak (kilowatt) possibilities, while the kilowatt-hour problem is set out in Table II. The data have been based on *normal prewar conditions in 1939*, when interconnection was being considered.

The system in each area is analyzed graphically (Figure 3) to show its maximum available generator capacity (*G*),

Table I. Output Data of Plants Associated With Interconnection

	Total Rated Turbine Output (Horsepower)	Size of Largest Unit (Horsepower)	Number of Units
Montreal area supplied chiefly by Montreal Light, Heat, and Power Consolidated	968,000	53,000	45
Shawinigan area supplied chiefly by Shawinigan Water and Power Company	1,089,500	44,500	48
Saguenay or Lake St. John area supplied chiefly by Aluminum Company of Canada, Ltd.	1,850,000	85,000	28
Miscellaneous Companies	80,000	6,000	20
Total	3,987,500 horsepower		

Maximum rated capacity operated in parallel (2,586,000 horsepower) 1,930,000 kw.

power available (P) as influenced by hydraulic conditions, and maximum firm load (L) which could be carried if operated as a completely independent system. To simplify the study, no allowance for reserve has been made to take care of load growth or frequency regulation.

From Figure 3A it may be noted that in the *Montreal area*, the comparatively steady power output from the St. Lawrence River plants having no storage facilities and the metropolitan load of low load factor are not an ideal combination from the point of view of matching available power facilities and load requirements. In the summer months, even at time of peak load, over 100,000 kw surplus output is indicated, while an additional 100,000 kw is available for about seven hours each day after midnight. This unfavorable situation results in surplus energy being available to an amount of over 800,000,000 kilowatt-hours per year (item 1, Table II; see also Figure 2).

In the *Shawinigan Area*, with its large storages and regulating possibilities on the St. Maurice River, it is possible to convert the entire available annual energy into firm load irrespective of daily or seasonal requirements (item 2, Table II). The curves (Figure 3B) also show that the maximum feasible firm load that may be carried at the normal 71 per cent yearly load factor is also just equal to the limit set by reduced system generator capacity during the high tailwater period.

Under these conditions, except for two months during the spring flood period, there is a minimum of about 60,000 kw reserve generator capacity. This reserve is admirably suited to assist in converting daily or seasonal surplus energy of adjacent areas into firm power by storing such surplus energy in the St. Maurice River storage reservoirs and returning it as firm power during the winter months to satisfy peak requirements. Additional generator capacity on the St. Maurice River would permit extension of this scheme.

In the *Saguenay Area*, using average runoff for the $4^1/_2$ winter months, the maximum possible firm load to be carried is dependent on the fixed storage capacity of Lake St. John. This results in a large amount of idle generator capacity during the winter months (Figure 3C) and large surplus summer energy (item 3, Table II).

The idle generator capacity during the winter months, combined with Lake St. John storage facilities, is useful in assisting in the conversion of surplus night energy in the Montreal area into firm power. Furthermore, part of the summer surplus energy may be stored in the reservoirs of the St. Maurice River and taken back during the winter months and thus be converted into firm power.

These curves indicate that if each of the systems in the three areas were to be operated independently:

(*a*). The firm loads which each system could carry successfully would be limited definitely to the values indicated by the load curves (see Figures 3A, 3B, 3C), on account of limiting local conditions.

(*b*). The equivalent energy (kilowatt-hours) consumed by such individual firm loads would be restricted to the values given in Table II.

Such isolated system operation would leave a fair portion of the combined total resources unused, or inefficiently used, as indicated by the following:

(*a*). 1.7 billion kilowatt-hours of idle energy (from a firm-power viewpoint) equal to over 18 per cent of the total energy available. This corresponds to 260,000 kw (74.6 per cent load factor) of unused potential firm power (Table II).

(*b*). Spare generator capacity and spare power could be made available to all areas at

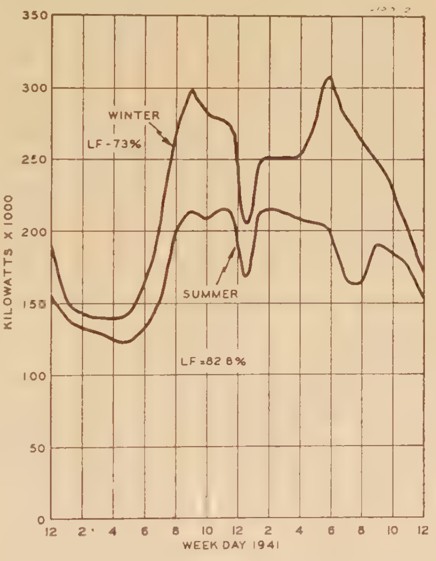

Figure 2. Typical summer and winter weekday (1941) firm load curves, Montreal area

Taken in combination with steady output of St. Lawrence River plants, the large amounts of surplus energy available for conversion into firm power are apparent

their critical periods during the year, because of the diversity in individual system power resources and load characteristics (Figure 3D).

These unused resources illustrate what potential possibilities a flexible interconnection of the three systems would offer for complete utilization of combined resources.

While it is not represented that this study is complete, and without attempting to describe in detail any specific coordination scheme or outline its practical execution, which would be most difficult without knowledge of how and where the reclaimed firm power would be used, it is submitted that the study does give a measure of the benefits to be derived from interconnection and justifies the following conclusion:

"By suitably interconnecting the three areas to permit free interchange of power, energy, and reserve generating facilities, by utilizing the Shawinigan facilities to store offpeak energy from neighboring areas, and by installing possibly 125,000 kw additional generating capacity on the St. Maurice River to permit returning the stored energy as firm power during the winter months, it is found possible to convert almost all of the surplus energy of the combined system into firm power.

"Thus a total of 260,000 kw of additional firm power may be gained (Table II, item 5), based on the combined system firm load factor under normal conditions of 74.6 per cent. This is illustrated by the curves Figure 3E. The capital expenditure involved for additional generating capacity and suitable tie-line connections has been estimated to be approximately \$38 per kilowatt of firm power reclaimed, as against \$100 to \$125 per kilowatt for new power developments."

Apart from the low cost and more efficient use of resources, it was realized that proper interconnection would be expected to result in better operating flexibility, increased service security, and a number of miscellaneous advantages, such as:

The actual situation in this respect in 1939 was:

(*a*). More complete utilization of surplus energy and less combined reserve generating capacity required.

(*b*). Better regulation of load, frequency, and voltage.

(*c*). Full advantage taken of diversity in load and hydraulic conditions.

(*d*). Better maintenance schedules of equipment possible.

(*e*). Deferment of individual system additions to generating plants.

V. Prewar Conditions

Although from many such studies and frequent discussions, the advantages of adequate system interconnection were well known, there was no economic justification for proceeding, during the prewar period, with the execution of any plan until the reclaimed power could be sold as firm power.

(*a*). Each system had large amounts of surplus power and energy available and was

able to derive some revenue from the sale of such power to electric boilers, while firm load growth was proceeding normally.

(*b*). Since the Montreal and Shawinigan areas were interconnected in a limited manner, a portion of the surplus energy in Montreal area was utilized for electric-boiler load on the Shawinigan system.

(*c*). The Shawinigan and Saguenay areas were not interconnected, but some 30,000 kw to 50,000 kw were being interchanged, when desirable, by transferring blocks of load from one area to the other on the 60-kv system.

(*d*). Companies had to abide by commitments and contractual obligations in regard to new plant construction, and, therefore, there was no incentive to defer construction programs and to obtain prime power from neighboring companies by interconnection.

VI. Changed Conditions Caused by the War

With the advent of the war, there was an urgent and immediate demand for large amounts of additional firm power for defense industries. This abnormal demand was not confined to any one area but was most pronounced in the Saguenay and Shawinigan areas. As a result, some form of co-ordination involving proper system interconnection was immediately sought because of the attractive prospect of providing additional firm power in the shortest time at minimum cost.

Compared to the studies previously made on a prewar basis, war conditions modified the situation to some extent, and the actual trend for full development and utilization of total power resources, was along the following lines:

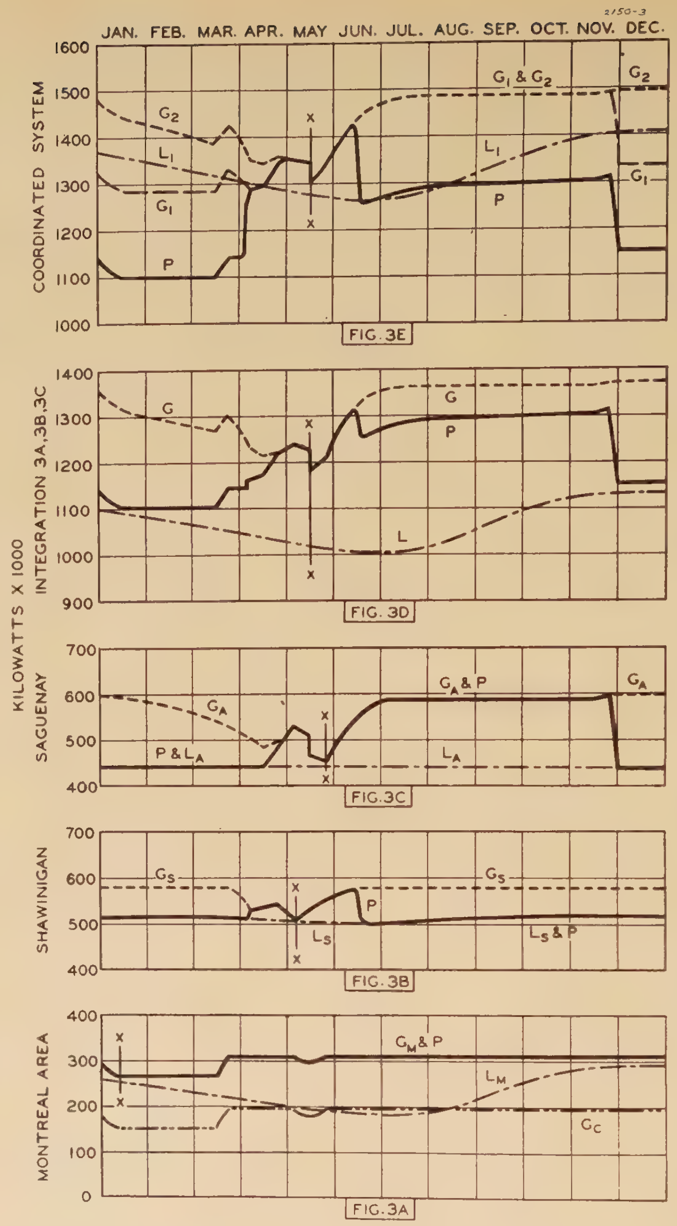

Figure 3. Curves to illustrate graphically the results of co-ordination study based on actual prewar system data

P=available firm power on a daily basis of at least normal system load factor. The variable shape of the curve is caused by water shortage or high tail-water conditions

G_M, G_S, G_A= maximum possible generator output at any instant on the individual Montreal, Shawinigan, and Saguenay systems respectively, as influenced by variable heads and flow conditions

G_C=actual Montreal area generator capacity, 1939, apart from Shawinigan contract

G=integration of G_M, G_S, and G_A, $G_2=G+125{,}000$-kw maximum system generator output with energy backing on a daily basis

$G_1=G_2-(G_A-P)$ =maximum system generator output with energy backing (normal-system firm load factor) from seasonal storage

L_M, L_S, L_A=maximum practical firm-power demand (at normal load factor) on the Montreal, Shawinigan, and Saguenay systems, respectively, and as dictated by maximum generator output at the critical point $X-X$ or shortage of water

These curves follow the typical or normal annual trend on the system concerned

L=integration of L_M, L_S, L_A

$L_1=L+260{,}000$ kw=estimated firm peak demand (at normal-system load factor) which could be successfully carried on the fully co-ordinated system

$X-X$=Critical points of generator reserve [since the Montreal Light, Heat, and Power Consolidated contract supply from Shawinigan Water and Power Company is duplicated in the output figures of each company (Figures 3A and 3B) a correction in the form of a deduction of 120,000 kw, (160,000 horsepower) has been made in the integrated curves (Figures 3D and 3E) to obtain the true result]

1. In the *Montreal area* there were immediately available some 825,000,000 kilowatt-hours of surplus energy annually which could be converted into prime power by utilizing Shawinigan storage. Such energy was available in the largest amounts during the summer months and during nights and week-

To utilize this energy necessitated increased back-feed from the 110-kv Montreal system and resulted in the installation (January 1942) of a 110-kv oil-filled underground cable seven miles long.[1] This eliminated the restriction or "bottleneck" in power flow through the 60-kv ring system and through the two 30,000-kva transformer banks in the city of Montreal and increased the transfer ability by over 100,000 kw.

2. In the *Shawinigan area* it was necessary to provide means for handling the increased feed-back from the Montreal area, as well as to store a portion of it, and transmit the remainder to the Saguenay area. This required certain changes on the system and included these items:

(*a*). Additional transformers and voltage regulating equipment were installed in certain districts to permit interchanging the full amount of power and to enable the reactive to be controlled.

(*b*). A number of additional storage projects were started on the St. Maurice River which, when completed, will provide additional capacity of 285 square-mile-feet equivalent to 16,000 horsepower years.

(*c*). To transmit the reclaimed firm power to the Saguenay area, a 220-kv transmission line was built and placed in operation December 1940. This line serves as a means of permanently interconnecting the Saguenay system with the Shawinigan and Montreal systems.

3. The *Saguenay Companies* decided on immediate industrial expansion and complete development and local utilization of power resources in the Saguenay area, as well as arranging for absorbing all available surplus power in the Shawinigan and Montreal areas. Physically, the program included:

(*a*). The construction of two storage reservoirs which, when completed, will provide the means of converting surplus summer energy in the Saguenay area into firm power. Storage capacities are estimated to be 2,460 and 5,360 square-mile-feet respectively and should permit an increase in the regulated flow of the Saguenay River from 32,000 to 45,000 cubic feet per second.

Table II. Study of Kilowatt-Hour Resources and Load Requirements

Based on Normal Prewar Conditions 1939

Item No.	Maximum Feasible Firm Loading If Operated as Isolated Systems: Annual Peak Demand, Kw	Annual Load Factor (Per Cent)	Equivalent Firm Energy Kwhr × 10^6	Total Practical Energy Available Annually Kwhr × 10^6	Surplus Energy Available Annually Kwhr × 10^6
1. Montreal area	293,000	47.5	1,220	2,045	825
2. Shawinigan area	520,000	71.0	3,240	3,600	360
3. Saguenay area	440,000	93.0	3,580	4,100	520
Total	1,253,000	73.0	8,040	9,745	1,705
* Less Montreal Light, Heat, and Power Consolidated contract	120,000		620	620	
4. Integration of items 1, 2, and 3. Net =	1,133,000	74.6	7,420	9,125	1,705

5. Combined surplus energy, if properly utilized and if converted into firm power, would be equivalent to:

$$\frac{1{,}705{,}000{,}000}{8{,}760 \times 0.746} = 260{,}000 \text{ kw}$$

6. Percentage of total energy resources reclaimable as firm power $= \frac{1{,}705}{9{,}125} = 18.6$ per cent

* Since Montreal Light, Heat, and Power Consolidated contract supply from Shawinigan Water and Power Company is duplicated in the output of each company, its deduction is necessary to obtain a true result.

(*b*). Completion of a second double-circuit 154-kv line.

The result has been that with the provision of these facilities a rapid and fairly complete exploitation of all available power and energy resources in these areas has been accomplished with minimum outlay and in minimum time, providing as well additional generator reserve, improved operating flexibility, and service security.

As an illustration of the actual results obtained in combined system output, a typical daily firm-power peak load curve (Figure 4) for January 1942 has been plotted and shows how fully the resources are being utilized for firm-power purposes. It is to be noted that on the individual systems the margin of generator reserve is very limited, whereas on the combined system fairly large reserves are available excepting at time of maximum system peak.

VII. Analyzer Studies and Increased Fault Currents

Anticipating the tying up of the Saguenay and Shawinigan systems over the 220-kv tie line, a number of studies were made on an a-c network analyzer to determine among other matters:

(*a*). The magnitude of fault current at different locations.

(*b*). Possibilities of automatic reclosing and single-phase switching on the 154-kv and 220-kv lines.

(*c*). Degree of stability.

These studies indicated that the interconnection materially increased the magnitude of the fault currents at the interconnecting points.

In most cases, existing oil circuit breakers were capable of meeting the increased duty, but in certain localities it was found necessary to modify existing breaker designs in order to increase the rupture duty and to reduce operating times.

Where new circuit breakers were found to be necessary, there was a tendency to adopt the air-blast breaker for 69-kv, 132-kv, and 220-kv service. The type of air-blast breaker generally adopted was developed by engineers of the Montreal Light, Heat, and Power, Consolidated after several years of development work and field tests. However, also on the system may be found European designs of 11-kv, 154-kv, and 220-kv air-blast circuit breakers, as well as 69-kv and 138-kv "oil-poor" breakers. Circuit-breaker performance is considered satisfactory, although in some instances it has been necessary to modify designs slightly.

VIII. Stability and High-Voltage Automatic Reclosing

In the matter of automatic reclosing of high-voltage lines, actual experience gained over a period of eight years with an installation on two long 187-kv lines indicates that under many transient faults returning the faulted circuit to service promptly by automatic means has prevented the loss of the second line due to the systems falling out of synchronism.

Studies on the analyzer of the Shawinigan-Saguenay 220-kv tie line indicated that with a line-to-ground fault on the line cleared in five cycles, using conventional three-phase switching, and reclosing automatically in 20 cycles, the resulting swing curves showed every indication that stability would be maintained. Since the subsequent line loading is much higher than that used in the studies, attention is now being directed to complete a study of single-phase switching. In due course it is expected that some definite progress will be made in a practical application.

On the two 187-kv circuits, now operating at 154 kv, a few cases of instability have been experienced caused primarily by line short circuits in the Saguenay area and under unusually heavy load conditions. Such operations were not unexpected, and, considering the transmission constants and excessive loading, completely stable operation under all types of faults is not anticipated.

Continual efforts are being made to improve system protection so as to reduce time of fault clearance, with the object of improving stability.

IX. 220-Kv/165-Kv Transformation

In order to connect the 220-kv Shawinigan system to the Saguenay system (operating at 154 kv to 165 kv) and to permit a load of 150,000 kw to be carried it was necessary to consider carefully the transformation problem.

Originally, two obvious ideas were considered:

(*a*). The installation of three 50,000-kva conventional double-winding 220-kv/165-kv three-phase transformers with a limited-capacity tertiary winding.

(*b*). The installation of two 75,000-kva 220-kv/165-kv three-phase autotransformers.

In the case of the conventional-type transformers, it was found that 50,000

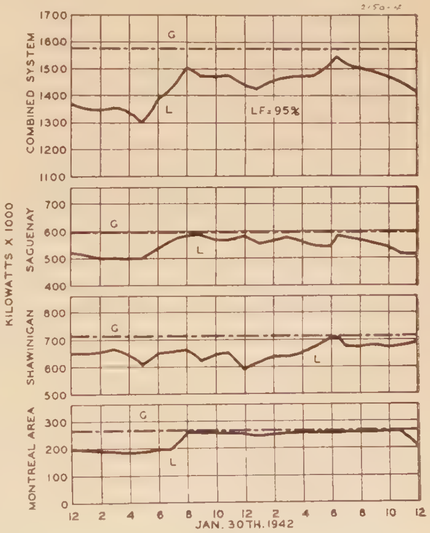

Figure 4. Actual daily load curve, interconnected systems, January 30, 1942, showing how available resources have been fully utilized for firm power purposes

kva was the largest size that could be handled from the point of view of transportation and servicing, and this would have required the purchase of three such units.

Regarding the use of autotransformers, because of their inherent lighter weight, it was possible to obtain from the manufacturer units of 75,000-kva rating so that only two transformers were required.

However, a third proposal was suggested, diagram of which is shown in Figure 5. This scheme was found to be practical by the manufacturer and was eventually used. The fundamental idea was to make use of the fact that there were already existing two 45,000-kva banks of 165-kv star to 66-kv delta. Since the problem was to transmit power from the 220-kv system to the 165-kv system, two three-phase "series" transformers were installed, each having a name-plate rating of 18,750 kva, 220 kv–55 kv/60 kv 196.8*A*, and supplied with a number of suitable voltage taps. The setup is really a scheme whereby use is made of the core structure of the existing 45,000-kva transformers in conjunction with the "series" transformers by electrically coupling at 60 kv, so as to simulate the effect of an autotransformer in the combined unit. By this means, the weight and rating of the additional transformer equipment may be considerably reduced without impairing the kilovolt-ampere transfer ability between 220 kv and 165 kv. Furthermore, the use of this idea results in a marked decrease in impedance and transformation losses between 220-kv, 165-kv, and 60-kv systems, as compared with the alternative schemes.

From the point of view of costs the percentages were:

Three 50,000-kva conventional-type three-phase transformers.........	100 per cent
Two 75,000-kva autotransformers..	70 per cent
Two 18,750-kva series transformers (150,000 kva equivalent)..........	57 per cent

More than a year's operation on these three-phase series banks has demonstrated the success of the arrangement and it has been found that loads of 190,000 kw have been transmitted from 220 kv to 165 kv without undue heating.

X. System Protection

When interconnecting large systems, the need for instantaneous and selective zone protection assumes special importance, to reduce the hazard of permanent power arc damage and system instability, since in many instances troubles pyramid.

Fortunately, on this high-voltage net-

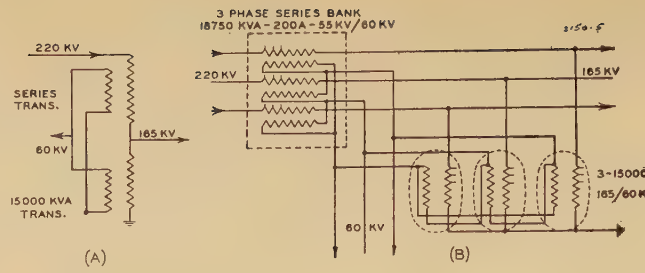

Figure 5. Diagram to illustrate method of interconnecting 220-kv system to 165-kv and 60-kv systems using series transformers

A. Development of one phase

B. 220-kv/165-kv/60-kv transformation

work and particularly on the Shawinigan system, the matter of such relay protection had been given unusual attention. The double step impedance principle, with its features of instantaneous fault clearance to within 80 per cent to 90 per cent of the line length, was conceived, developed, and applied as early as 1920 for the clearance of phase-to-phase and ground faults. General application of the instantaneous differential-current zone protection, to generators, transformers, and station bus bars quickly followed.

With the adoption of high-voltage network distribution came the problem of instantaneous protection for the short tie lines. This led to the development and successful application of a number of differential-current pilot-wire schemes, using a separate control cable for the pilot wires. Subsequently, the directional impedance interlock principle was developed, with available communication circuits being used as a control channel.

Recently commercial carrier-current relaying, combined with a simplex communication, has been used in conjunction with some important high-voltage trunk lines to provide selective instantaneous clearance.

For transformer and regulator protection, in addition to differential schemes and supplemented by residual current features, gas-detector relays are being used. The pressure element is arranged to trip the associated circuit breakers, while the gas element operates an alarm.

For standby or backup protection, the impedance principle has been adopted almost exclusively since its initial development because of its effectiveness under all operating conditions and irrespective of the amount of system generator capacity connected. This is in contrast to the shortcomings, in these respects, of the inverse definite time overload protection previously used.

At controlling points, automatic oscillographs and high-speed recording voltmeters are utilized, as well as transmission-line fault locators, to ensure sufficient available data so that each case of trouble may be fully investigated and proper remedial measures applied.

To ensure proper over-all functioning of the protective schemes, the practice of making "primary" checks and tests on new protection installations is standard.

Concluding this section, it is apparent that without the progress and development just outlined in regard to protective schemes, interconnection of systems would have been a hazardous venture with added service difficulties, rather than the increased service reliability actually obtained.

XI. Basic System-Operating Principles

The general principles underlying the operation of the combined systems may be summed up as follows:

(*a*). To assure satisfactory parallel operation, each company undertakes as far as possible and with a practical spirit of co-operation to maintain load, voltage, and frequency conditions which are most suitable to the common interest, keeping in mind, of course, local problems and obligations to customers.

(*b*). Firm power contracts existing prior to interconnection remain in full force and effect.

(*c*). Operating agreements have been made to cover the supply or purchase of surplus energy.

(*d*). It is the objective of all companies concerned to ensure maximum utilization of all available power resources to assist in the war effort. Equitable energy rates have been agreed upon to make this result possible.

So far, any unusual operating matters which have arisen and which were not covered by agreement, have been promptly and satisfactorily rectified by the operating managements of the respective companies.

XII. Control of Voltage and Reactive

Constant voltage is required at the load in the Saguenay and Montreal areas and on the 60-kv busses. In addition, however, when the following items have

been considered, it will be realized that a definite reactive control problem exists:

(*a*). Between the Montreal area and the Shawinigan area, involving the four 110-kv circuits, the load may have to vary between 120,000 kw *delivered to* Montreal and 130 kw *fed back from* Montreal.

(*b*). The load transmitted over the upper St. Maurice 220-kv line varies from 100,000 kw to 250,000 kw and is delivered at the receiving point at approximately 100 per cent power factor, so that most of the reactive kilovolt-amperes of the two generating plants feeding this line is used in transmission and is not available therefore for the load.

(*c*). The load transmitted from Quebec to Lake St. John in the Saguenay area may vary between 25,000 kw and 150,000 kw, and at Lake St. John it is frequently necessary to supply 50,000 kva; that is, the power is received at a leading power factor.

(*d*). System load reductions of the order of 45 per cent occur each Sunday when mills shut down.

(*e*). Only one synchronous condenser (30,000-kva capacity) is available on the system installed at Quebec.

This whole matter of voltage and reactive control was given attention in the a-c network-analyzer studies, and as a result, it was decided to install "on-load" tap-changing gear on all transformers in the Shawinigan area where a tie exists between the 220-kv system and the 110-kv Montreal system, as well as on a customer's 110-kv bank.

So far, with some extra supervision required, use of these facilities has enabled voltage levels to be maintained by proper generator field manipulation, as well as by occasional use of vented hydroelectric units as synchronous condensers, and by changing taps at controlling points when dictated by marked changes in loading conditions.

XIII. Frequency Regulation, Load Control, and Reserve Generation

Normally, generating stations in the Montreal area are operated on base load, with frequency controlled by either Shawinigan or Saguenay systems. Automatic frequency control is available and is used on two generating stations in the Shawinigan area. With manual control no difficulty has been experienced in holding frequency to plus or minus 0.1 cycle and synchronous time to a maximum error of ten seconds. This may not be as close regulation as many other systems obtain but is found to be quite satisfactory in this case. Manual control of frequency has been made more convenient to the operator by bringing the governor motor control to miniature keys on the operator's desk.

Automatic tie-line load control is not used and is not considered essential at the present time, although it would be a definite operating convenience. The long distances between exchange points and generating stations make the application of automatic tie-line control both difficult and expensive.

At the generating station on frequency control, it has been the custom in the past to try to maintain a minimum amount of spare equivalent to a 30,000-kw unit during the peak period, and to retain in service at other generating stations a sufficient number of units to carry the allotted load efficiently, consistent with flow and head conditions, so that when the need arose, an additional 30,000 kw–50,000 kw could be picked up by running wide open temporarily. However, present practice is to utilize, as far as possible, most of the normal generator reserve for supplying urgent defense loads. This results occasionally in the necessity of dropping blocks of electric furnace load or grinder motors as a temporary means of maintaining suitable frequency.

XIV. Summary and Conclusions

1. The interconnected system described and supplied solely from hydroelectric plants must be considered as one of the major power systems on the continent, having resources of nearly 4,000,000 horsepower (developed or under construction), of which 2,586,000 horsepower are now operating in parallel.

2. It is to be noted that while operating as individual systems or areas, the hydraulic power resources and load requirements of each group were not ideally matched, it was found that when viewed as a combined system, these characteristics were quite complementary, thus enabling much more complete firm-power utilization of total energy resources.

3. Under normal prewar conditions, undoubtedly this interconnection of systems would have been delayed in its execution, but because of the immediate demand for more firm power for defense industries, it became imperative and economically feasible to proceed, therefore in effect making available for firm power over 18 per cent of the total combined energy resources which otherwise would have been wasted or sold as secondary power to electric boilers. This estimated gain in firm power output is apart from that obtained from additional sources as given in section VI.

4. Because of the necessity of producing and utilizing every available kilowatt-hours for defense purposes, operating procedure has changed, resulting in larger ranges in frequency and voltage regulation and less generator reserve.

Control of reactive kilovolt-amperes is successfully accomplished mainly by generator field adjustment and use of transformer tap-changing equipment, with the assistance of a single 30,000-kva synchronous condenser.

Reference

1. Design, Manufacture, and Installation of 120-Kv Oil-Filled Cables in Canada, **D. M. Farnham, O. W. Titus.** AIEE Transactions, volume 61, 1942, December section, pages 881–8.

Three-Winding Transformer Ring-Bus Characteristics

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SUCH hydroelectric projects as the Boulder Dam, Grand Coulee, and that proposed for the St. Lawrence River present serious power limit and short-circuit problems. Each of these developments will be capable of producing over 1,000,000 kw, part of which for each case must be transmitted over 200 miles. Any method which will increase transient stability and lower fault currents should be carefully investigated; this paper presents the investigation of such a method.

Statement of Problem

To obtain optimum use of transmission lines between a generating station and a load center, it is desirable to parallel the circuits in such a manner that the loss of a line or section of a line will result in the least disturbance. In general, this is done by bussing the lines together and sectionalizing at one or more points.

However, to bus lines on the sending and receiving ends imposes very severe requirements on switching equipment. For example, the 287.5-kv Boulder Dam–Los Angeles lines require 287.5-kv breakers with interrupting capacities of 2,500,000 and 3,500,000 kva. Even on 230-kv systems circuit breakers with interrupting capacities of 2,500,000 kva are in use.

High-voltage bus reactors may be used to reduce short-circuit currents, but besides being costly, they tend materially to increase the reactance between the sending and receiving ends of the system after the loss of line or line section. It is probable, even though the fault currents on the circuits are reduced by the addition of reactors to the bus, that the dynamic power limit would be decreased because of the added impedance.

The use of reactors in the transformer neutrals to reduce ground-fault currents may interfere with relaying and imposes higher-voltage stresses on equipment. Also, for three-phase faults neutral reactors have no effect on fault currents and thus do not increase the power limit for such disturbances.

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Description of Studies

Three-winding transformers connected so as to form a ring on the low-voltage side, as shown in Figure 1, lower short-circuit currents by making use of the reactance between low-voltage windings. To determine the transient power limits and fault currents of a system using this ring-bus arrangement, studies were made on an a-c network analyzer. For comparison similar studies were made on the same system with a standard high-voltage bus, as shown in Figure 2.

The studies prove that the transient stability limits are higher and the fault currents are lower for the three-winding transformer ring-bus system. A very interesting result was that the transient stability limits for the above system are almost independent of the reactance between low-voltage windings as it varies from zero to 40 per cent.

The basic system for the studies consisted of four 230-kv transmission lines 185 miles in length connecting a hydroelectric plant to a load center. The load center was connected through a short line to an infinite bus. The generating plant consisted of eight identical 60-cycle machines, each of 55,000-kva capacity. Each generator had a direct-axis transient reactance of 44 per cent and a WR^2 of 132,500,000 pound-feet squared.

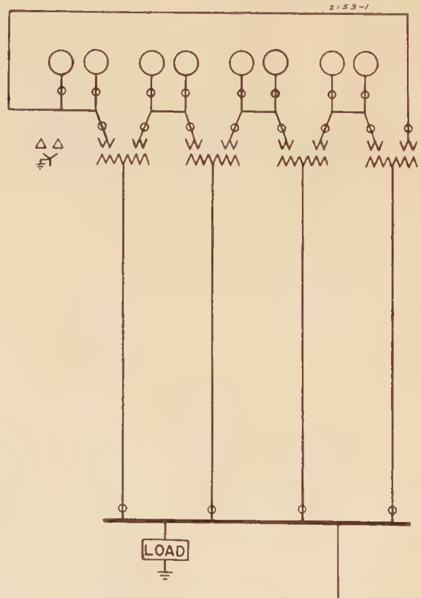

Figure 1. Schematic diagram for the three-winding transformer bus system

For the three-winding transformer ring bus the transformer banks were rated 110,000/110,000/150,000 kva at 13.8/13.8/230 kv and connected delta, delta, wye-grounded. With the low-voltage windings paralleled, the through reactance of the transformers was 11 per cent on a 150,000-kva base.

For the standard bussing arrangement the transformer banks were rated at 110,000 kva, 13.8/230 kv and connected delta, wye-grounded. The reactance was 11 per cent on a 110,000-kva base. Transformer banks rated at 110,000 kva but having a reactance of 11 per cent on a 150,000-kva base were also used.

The various sytems were placed on an a-c network analyzer and studies made of short-circuit and transient-stability characteristics. Faults were applied on a generator bus and also on the sending end of a transmission line. For the three-winding transformer ring bus the reactance between the low-voltage windings was varied from zero to 40 per cent.

The results of the short-circuit studies, shown in Figure 3, indicate that the severity of three-phase short circuits on the generator bus of the transformer ring-bus system can be made as low as that of the standard bus system. This is accomplished by using low-voltage windings having a reactance between them of at least 42 per cent.

By not bussing the transformers on the high-voltage side, the severity of faults on the transmission lines near the generating station are very greatly reduced. This materially reduces system disturbances resulting from the line faults.

Transient-stability limits for the two standard bus systems were found for both three-phase and two-phase faults on the lines near the sending end. For these and all stability studies a total clearing time of $4^1/_2$ cycles was assumed.

Similar studies were made on the three-winding transformer bus system as the reactance between the low-voltage windings was varied from zero to 40 per cent.

The power-limit values from Figure 4 show that the standard systems are much less stable in all cases. For example, the best standard system carries at least 36,000 kw less than can the transformer ring-bus system.

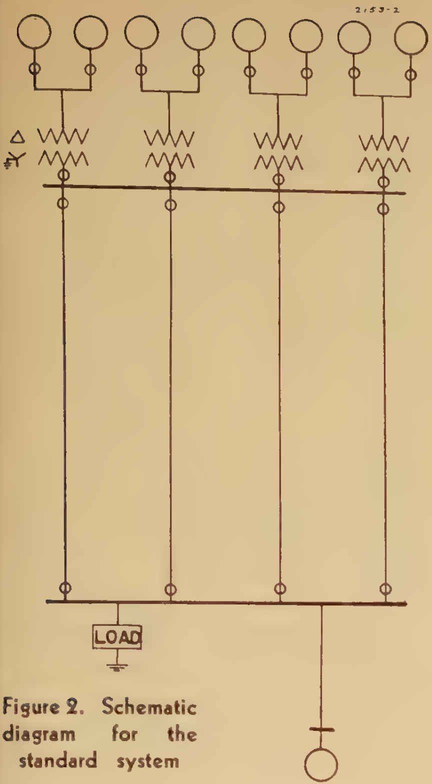

Figure 2. Schematic diagram for the standard system

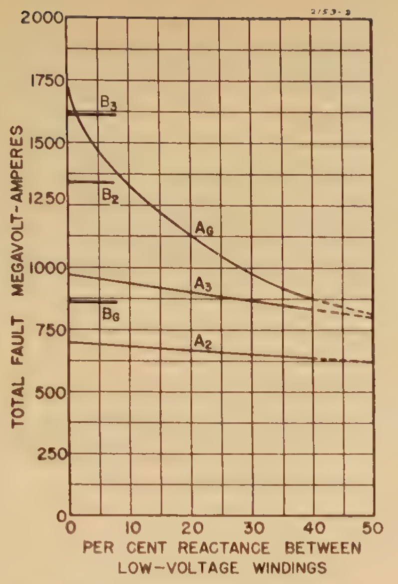

Figure 3. Short-circuit characteristics

Curves *A* for the three-winding transformer ring-bus system as follows:

A_G—Three-phase fault on a generator bus

A_3—Three-phase fault on transmission line near sending end

A_2—Double-line-to-ground fault on a transmission line near sending end

Curves *B* for the standard bus system with 11 per cent transformers on a 110,000-kva base as follows:

B_G—Three-phase fault on a generator bus

B_3—Three-phase fault on a transmission line near sending end

B_2—Double-line-to-ground fault on a transmission line near sending end

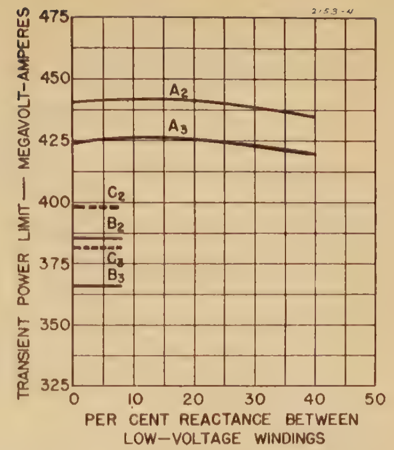

Figure 4. Transient-stability limits

Curves *A* for the three-winding transformer ring-bus system as follows:

A_3—Three-phase fault on transmission line near sending end

A_2—Double-line-to-ground fault on transmission line near sending end

Curves *B* for the standard system with 11 per cent transformers on a 110,000-kva base as follows:

B_3—Three-phase fault on transmission line near sending end

B_2—Double-line-to-ground fault on a transmission line near sending end

Curves *C* for the standard system with 11 per cent transformers on a 150,000-kva base

C_3—Three-phase fault on a transmission line near sending end

C_2—Double-line-to-ground fault on a transmission line near sending end

It should be kept in mind that the assumed clearing time is almost the minimum possible for 230-kv systems. Longer clearing times would increase the difference in stability limits between the systems and so favor the ring-bus system.

The rise in power limits for the ring-bus system as the transformer reactance increases from zero to 15 per cent is explained by the decreasing severity of the faults. At 15 per cent reactance the effect of the bus reactance in decreasing synchronizing power between the generators becomes larger than that due to decreased fault currents, and the power limits tend to drop when the reactance is increased. However, the variation in power limit is small, being less than two per cent over the entire range; thus higher reactances between low-voltage windings may be used, resulting in greatly reduced currents for generator bus faults.

Conclusions

While a detailed economic study is beyond the scope of this paper, it may be mentioned that three-winding transformers with the ranges of reactance used in this study may be obtained for the same price as standard three-winding transformers. The increase in cost of the three-winding transformers over the smaller capacity two-winding transformers is partially offset by the elimination of high-voltage breakers.

However, on transient stability and short-circuit considerations the three-winding transformer ring bus offers very definite advantages for systems of large magnitudes.

Inverse Functions of Complex Quantities

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FELLOW AIEE

FORMULAS for inverse functions of complex quantities, such as $\sin^{-1}(x+iy)$, are of use in several branches of electrical engineering. Calculations for transmission circuits require them, particularly in connection with communication circuits. Integration of expressions involving complex quantities can involve inverse functions. They are encountered also in conformal transformations.

In presenting the formulas in this paper, it is necessary to specify angles with sufficient detail to avoid ambiguity and the liability of incorrect results. Also, all of the appropriate multiple values should be included. It is not always obvious by inspection that two different complex values are only values of different branches of the same function and are both correct.

In all cases, numerical examples are given as illustrations.

Inverse Sine $\sin^{-1}(x \pm iy)$

Let

$$\sin^{-1}(x+iy)=u+iv$$

where x, y, u, and v are real quantities and $i=\sqrt{-1}$.

$$\sin(u+iv)=(x+iy)$$

$$\sin u \cosh v = x \tag{1}$$

$$\cos u \sinh v = y \tag{2}$$

Squaring equations 1 and 2 and putting

$$\sin^2 u = 1-\cos^2 u$$

and

$$\cosh^2 v = 1+\sinh^2 v$$

there is obtained, by eliminating $\sinh^2 v$ $\left(\text{put } \sinh^2 v = \dfrac{y^2}{\cos^2 u}\right)$

$$\cos^4 u-(1-x^2-y^2)\cos^2 u-y^2=0 \tag{3}$$

from which

$$\cos^2 u = \tfrac{1}{2}[1-x^2-y^2 \pm \sqrt{(1-x^2-y^2)^2+4y^2}] \tag{4}$$

Since u is real and $\cos^2 u$ is positive, the positive value of the root is taken.

The quantity under the radical sign may be factored, and is

$$\{(1+x)^2+y^2\}\{(1-x)^2+y^2\} \tag{5}$$

From equations 4 and 5

$$\sin^2 u = \tfrac{1}{2}[1+x^2+y^2-\sqrt{\{(1+x)^2+y^2\}\{(1-x)^2+y^2\}}]$$
$$=\tfrac{1}{4}[\sqrt{(1+x)^2+y^2}-\sqrt{(1-x)^2+y^2}]^2$$

$$\sin u = \pm\left(\frac{p-q}{2}\right)=\frac{\pm 2x}{p+q} \tag{6}$$

where $p=\sqrt{(1+x)^2+y^2}$ (positive value) (7)

$q=\sqrt{(1-x)^2+y^2}$ (positive value) (8)

The second solution in equation 6 is obtained by rationalizing the numerator. It involves the sum of two quantities instead of the difference and so allows more convenient precise computation.

Since v is real, $\cosh v$ is positive, as may be seen from the series expansion. Therefore, by equation 1, $\sin u$ is the same sign as x. This allows $\pm$ in equation 6 to be changed to $+$. Therefore

$$u=\sin^{-1}\frac{2x}{p+q} \tag{9}$$

Take the principal value of u, that is, the value between $-\pi/2$ and $\pi/2$. Since u lies in this range, $\cos u$ is positive.

The following expression corresponding to equation 3 can be obtained by eliminating $\cos^2 u$

$$\sinh^4 v+(1-x^2-y^2)\sinh^2 v-y^2=0$$

$$\sinh^2 v = \tfrac{1}{2}[x^2+y^2-1\pm\sqrt{(1-x^2-y^2)^2+4y^2}]$$

The quantity under the radical sign is the same as equation 5 so that

$$\cosh v = \pm\tfrac{1}{2}(p+q)$$

Since v is real, the positive value is to be taken.

$$v=\cosh^{-1}\frac{p+q}{2} \tag{10}$$

Since $\cos u$ is positive, then by equation 2, $\sinh v$ and v are the same sign as y. This may be secured by writing

$$\sin^{-1}(x\pm iy)=\sin^{-1}\frac{2x}{p+q}\pm i\cosh^{-1}\frac{p+q}{2}$$

where y is positive and the positive value of $\cosh^{-1}$ is taken (see reference 1, page 58, and reference 2, page 264).

For any angle θ, there are angles $2k\pi+\theta$ which are the same as θ in all respects, and there are angles $(2k+1)\pi-\theta$ which have the same sine, where k is a positive or negative integer or 0. These can be combined by stating that angles having the same sine as θ are

$$n\pi+(-1)^n\theta \tag{11}$$

where n is an integer or zero. Therefore

$$\sin^{-1}(x\pm iy)=n\pi+(-1)^n\sin^{-1}\frac{2x}{p+q}\pm i(-1)^n\cosh^{-1}\frac{p+q}{2} \tag{12}$$

taking the principal value of $\sin^{-1}$ and the positive values of $\cosh^{-1}$ and of the radicals p and q. The quantity x may be positive or negative, but the quantity y is positive.

Note that if $y=0$ and $x>1$, $q=x-1$ and $p+q=2x$. If $y=0$ and $x<1$, $q=1-x$ and $p+q=2$.

An alternative computation may be made by means of the well-known formula

$$\sinh^{-1} m = \log n\,(m+\sqrt{m^2+1}) \tag{13}$$

where logn denotes natural logarithm. Let

$$\sin^{-1} A = u$$

where A and u are complex quantities

$$\sin u = A$$
$$iA = \sinh iu$$
$$iu = \sinh^{-1} iA$$
$$\sin^{-1} A = -i\sinh^{-1} iA+2k\pi$$
$$=-i\log n\,(\pm\sqrt{1-A^2}+iA)+2k\pi \tag{14a}$$
$$\text{or } =i\log n\,(\pm\sqrt{1-A^2}-iA)+2k\pi \tag{14b}$$

The two solutions of equation 14a indicated by $\pm$ correspond to the two angles θ and $\pi-\theta$ which have the same sine. The second or alternative form, equation 14b, is identical with equation 14a. The expression involving $+$ in equation 14b is obtained from that involving $+$ in equation 14a by rationalizing the numerator in equation 14a. In practice, the form should be used which involves the numerical sum of quantities instead of the difference, thus giving more convenient precise computation.

Square Root $\sqrt{(x\pm iy)}$

In the computation just described, the square root of a complex quantity is required. It may be expressed as follows:

$$\sqrt{(x+iy)}=\pm\left[\sqrt{\left(\frac{m+x}{2}\right)}+i\sqrt{\left(\frac{m-x}{2}\right)}\right] \tag{15}$$

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$$\sqrt{(x-iy)} = \pm\left[\sqrt{\left(\frac{m+x}{2}\right)} - i\sqrt{\left(\frac{m-x}{2}\right)}\right] \quad (16)$$

where x may be positive or negative, y is positive and

$$m = +\sqrt{(x^2+y^2)} \quad (17)$$

The positive square roots of $(m+x)/2$ and $(m-x)/2$ are used (see reference 2, page 260).

An alternative method is to express the complex quantity in the polar form

$$r\angle\theta = re^{i\theta} = r(\cos\theta + i\sin\theta) \quad (18)$$

where, if the complex quantity is $x+iy$

$$r = +\sqrt{(x^2+y^2)},\ \cos\theta = \frac{x}{r} \text{ and } \sin\theta = \frac{y}{r} \quad (19)$$

Then

$$\sqrt{(x+iy)} = \pm\sqrt{r}\underline{/\theta/2}$$
$$= \pm\sqrt{r}\left(\cos\frac{\theta}{2} + i\sin\frac{\theta}{2}\right) \quad (20)$$

The angle θ may be in any one of the four quadrants, depending on whether x and y are positive or negative quantities. The angle is not specified according to the principal values of $\tan^{-1}$ and $\cos^{-1}$, and so forth, though the numerical value of θ may be conveniently found by using a table of tan or $\tan^{-1}$ and then determining the quadrant for θ by equation 19.

Logarithm log$_n$ (x+iy)

In using equation 14, the logarithm of a complex quantity is required. This is computed as an inverse function of e^z.

Let

$$\log_n(x+iy) = u+iv$$
$$e^{u+iv} = e^u(\cos v + i\sin v)$$
$$= x+iy$$

$$e^u\cos v = x \quad (21)$$

$$e^u\sin v = y \quad (22)$$

Squaring and adding

$$e^{2u} = (x^2+y^2)$$

Let

$$r = \sqrt{x^2+y^2}$$

The positive value of the root is to be taken since u is real and e^u is positive.

The angle v is to be specified with sufficient completeness so that the numerical values of cos v and sin v will have the correct signs.

$$\log_n(x+iy) = {}^1/_2\log_n(x^2+y^2) + i(\theta+2\pi k) \quad (23)$$

where $\cos\theta = x/r, \sin\theta = y/r, r = \sqrt{(x^2+y^2)}$ and k is an integer or 0, the positive value of r being taken. The quantities x and y may be positive or negative (see reference 7, page 3).

The angle θ, according to this specification, is not always a principal value of $\cos^{-1}$, $\sin^{-1}$, or $\tan^{-1}$. If both x and y are negative, tan θ is positive and the angle θ is in the third quadrant.

Another case where

$$\theta = \tan^{-1}\frac{m}{n}$$

is not a sufficient specification is in the equation

$$m\cos A + n\sin A = r\sin(A+\theta) \quad (24)$$

where $r = \sqrt{m^2+n^2}$, $\sin\theta = m/r$ and $\cos\theta = n/r$

(See reference 5, number 401.2).

Example 1. $\sin^{-1}(2+i3)$

$$p = \sqrt{(9+9)} = 4.243$$
$$q = \sqrt{(1+9)} = 3.162$$
$$p+q = 7.405$$

$$\sin^{-1}\frac{4}{7.405} = \sin^{-1}0.540 = 0.570 \text{ radian}$$

$$\cosh^{-1}\frac{7.405}{2} = 1.983$$

Putting $n=0$ in equation 12,

$$\sin^{-1}(2+i3) = 0.570 + i1.983$$

Putting $n=1$

$$\sin^{-1}(2+i3) = 3.142 - 0.570 - i\,1.983$$
$$= 2.572 - i\,1.983$$

In this computation, tables of $\sin^{-1} x$, $\cosh^{-1} x$, and so forth, as in reference 6, for real values of x, may be used. To check,

$$\sin(0.570 + i\,1.983) = 0.540\times3.702 + i\,0.842\times3.563$$
$$= 2.00 + i\,3.00$$

and

$$\sin(2.572 - i1.983) = 0.540\times3.702 - i(-0.842\times3.563)$$
$$= 2.00 + i\,3.00$$

If the only purpose of the real part of the value of $\sin^{-1}(2+i3)$ is to take the sin, cos, or tan, or to add it to other angles which are given in degrees, then a trigonometric table in degrees might be used, but care would be needed in choosing an appropriate notation.

In using equation 14 to obtain $\sin^{-1}(2+i3) = \sin^{-1} A$

$$1-A^2 = 6 - i\,12$$
$$= x - iy \qquad \text{as in equation 16}$$
$$m = \sqrt{(36+144)} = 13.42 \text{ by equation 17}$$
$$m+x = 19.42,\ m-x = 7.42$$
$$\sqrt{(1-A^2)} = \sqrt{\left(\frac{19.42}{2}\right)} - i\sqrt{\left(\frac{7.42}{2}\right)}$$

by equation 16, using the + sign

$$= 3.116 - i\,1.926$$
$$-iA = 3 \qquad -i\,2$$
$$\sqrt{(1-A^2)} - iA = 6.116 - i\,3.926$$

Equation 14b is used instead of equation 14a so as to avoid the small difference of nearly equal quantities.

Let $6.116 - i3.926 = x+iy$ as in equation 23

$$6.116^2 = 37.4$$
$$3.926^2 = 15.4$$
$$52.8$$

$${}^1/_2\log_n 52.8 = {}^1/_2(1.664+2.303) = 1.983$$

The angle θ of equation 23 is in the fourth quadrant.

$$\tan(-\theta) = \frac{3.926}{6.116} = 0.642$$

$$\theta = -0.571 \text{ radian}$$

$$\log_n(x+iy) = 1.983 - i0.571 \qquad \text{by equation 23}$$

$$\sin^{-1}(2+i3) = 0.571 + i1.983 \qquad \text{by equation 14b}$$

The second solution is given by the − sign in equation 14a or 14b, the former being preferable.

$$-\sqrt{(1-A^2)} + iA = -6.116 + i3.926$$

The angle θ of equation 23 is in the second quadrant.

$$\sin^{-1}(2+i3) = 2.571 - i1.983$$

The quantity $2k\pi$ may be added to either of these solutions.

Inverse Cosine $\cos^{-1}(x \pm iy)$

Let $\cos^{-1}(x+iy) = u+iv$

$$\cos(u+iv) = x+iy$$

$$\cos u\cosh v = x \quad (25)$$

$$\sin u\sinh v = -y \quad (26)$$

Squaring and eliminating $\sinh^2 v$,

$$\sin^4 u - (1-x^2-y^2)\sin^2 u - y^2 = 0$$

$$\sin^2 u = {}^1/_2[1-x^2-y^2 \pm \sqrt{(1-x^2-y^2)^2+4y^2}]$$

Since u is real and $\sin^2 u$ is positive, use the positive value of the root.

$$\cos^2 u = {}^1/_2(1+x^2+y^2-pq)$$
$$\cos u = \pm{}^1/_2(p-q)$$

Since v is real, cosh v is positive, and cos u is the same sign as x, by equation 25. Then

$$\cos u = {}^1/_2(p-q) = \frac{2x}{p+q}$$

$$u = \cos^{-1}\frac{2x}{p+q}$$

Take the principal value, that is, the value between 0 and π. Then $\sin u$ is positive. Similarly

$$\sinh^4 v+(1-x^2-y^2)\sinh^2 v-y^2=0$$
$$\sinh^2 v={}^1/_2[x^2+y^2-1+\sqrt{(1-x^2-y^2)^2+4y^2}]$$

taking the positive value of the root since $\sinh^2 v$ is positive.

$$\cosh^2 v={}^1/_4(p+q)^2$$
$$\cosh v={}^1/_2(p+q)$$

taking the positive value since v is real.

$$v=\cosh^{-1}\frac{p+q}{2}$$

Since $\sin u$ is positive, $\sinh v$ and v are the same sign as $-y$, from equation 26.

$$\cos^{-1}(x+iy)=\pm\left[\cos^{-1}\frac{2x}{p+q}+2k\pi-i\cosh^{-1}\frac{p+q}{2}\right] \quad (27)$$

where y is positive, taking the principal value of $\cos^{-1}$ and the positive values of $\cosh^{-1}$ and of p and q. Also

$$\cos^{-1}(x-iy)=\pm\left[\cos^{-1}\frac{2x}{p+q}+2k\pi+i\cosh^{-1}\frac{p+q}{2}\right] \quad (28)$$

taking the same values of $\sin^{-1}$ and $\cosh^{-1}$ as with equation 27. For p and q see equations 7 and 8.

The quantity x may be positive or negative. The quantity y is positive.

An alternative method is by use of the equation

$$\cosh^{-1} p=\pm\operatorname{logn}(x+\sqrt{x^2-1})$$

Let $\cos^{-1} A=u$, a complex quantity.

$$A=\cos u=\cosh iu$$
$$iu=\cosh^{-1} A$$

$$\cos^{-1} A=-i\cosh^{-1} A=$$
$$\mp i\operatorname{logn}(A+\sqrt{A^2-1})+2k\pi \quad (29a)$$
$$\text{or } =\pm i\operatorname{logn}(A-\sqrt{A^2-1})+2k\pi \quad (29b)$$

The second equation is obtained by rationalizing the numerator of the first and is to be used when it avoids the numerical difference of quantities.

Example 2. $\cos^{-1}(-2-i4)$

By equation 28, putting $k=0$

$$\cos^{-1}(-2-i4)=\pm[\cos^{-1}(-0.4385)+i\cosh^{-1}4.56]$$
$$=\pm[2.025+i2.198]$$

To check,

$$\cos(2.025+i2.198)=-0.4385\times4.56-i0.899\times4.45$$
$$=-2.00-i4.00$$

By the alternative method, let $A=-2-i4$

$$A^2-1=-13+i16$$
$$\sqrt{(A^2-1)}=1.952+i4.10$$
$$A=-2-i4$$

Using equation 29b to avoid the numerical difference of quantities

$$A-\sqrt{(A^2-1)}=-3.952-i8.10$$

In finding the logarithm of this, $\operatorname{logn} r=2.20$, and the angle θ is in the third quadrant and is 4.258 radians.

$$\cos^{-1}(-2-i4)=\pm(-4.258+i2.20)+2k\pi$$
$$=\pm(2.025+i2.20)$$

putting

$$k=1.$$

Inverse Tangent $\tan^{-1}(x+iy)$

Let $\tan^{-1}(x+iy)=u+iv$

$$\tan(u+iv)=x+iy=i\tanh\frac{u+iv}{i}$$

since

$$\tan z=i\tanh(z/i)$$

$$\tanh\frac{u+iv}{i}=\frac{x+iy}{i}$$

$$v-iu=\tanh^{-1}(y-ix)$$

$$={}^1/_2\operatorname{logn}\frac{1+y-ix}{1-y+ix} \quad (30)$$

where logn denotes natural logarithm.

$$e^{2v}(\cos 2u-i\sin 2u)=\frac{1+y-ix}{1-y+ix}$$

Rationalizing the denominator

$$e^{2v}\cos 2u=\frac{1-x^2-y^2}{(1-y)^2+x^2} \quad (31)$$

$$e^{2v}\sin 2u=\frac{2x}{(1-y)^2+x^2} \quad (32)$$

Squaring, adding, and factoring the numerator

$$e^{4v}=\frac{(1+y)^2+x^2}{(1-y)^2+x^2} \quad \text{by equation 5}$$

$$v=\frac{1}{4}\operatorname{logn}\frac{(1+y)^2+x^2}{(1-y)^2+x^2} \quad (33)$$

Dividing equation 32 by equation 31

$$\tan 2u=\frac{2x}{1-x^2-y^2} \quad (34)$$

Let

$$2u=\pi-\tan^{-1}\frac{1+y}{x}-\tan^{-1}\frac{1-y}{x} \quad (35)$$
$$=\pi-\alpha-\beta$$

where the principal values of $\tan^{-1}$ are taken, that is, α and β are between $-\pi/2$ and $\pi/2$.

$$\sin(\pi-\alpha-\beta)=\sin(\alpha+\beta)$$
$$=(\tan\alpha+\tan\beta)\cos\alpha\cos\beta$$
$$=\frac{2}{x}\cos\alpha\cos\beta$$

which is the same sign as x, as it should be, from equation 32. $\cos\alpha$ and $\cos\beta$ are positive.

$$\cos(\pi-\alpha-\beta)=-\cos(\alpha+\beta)$$
$$=(-1+\tan\alpha\tan\beta)\cos\alpha\cos\beta$$
$$=(1-x^2-y^2)\frac{\cos\alpha\cos\beta}{x^2}$$

which is the same sign as $1-x^2-y^2$ and proportional to it (see equation 31).

The quantity $2k\pi$, where k is an integer, may be added to equation 35.

Therefore

$$\tan^{-1}(x+iy)=\frac{1}{2}\left\{(2k+1)\pi-\tan^{-1}\frac{1+y}{x}-\tan^{-1}\frac{1-y}{x}\right\}+\frac{i}{4}\operatorname{logn}\frac{(1+y)^2+x^2}{(1-y)^2+x^2} \quad (36)$$

where the principal values of $\tan^{-1}$ are taken and where x and y may be positive or negative.

An alternative method of computation is by means of equation 31

$$\tan^{-1}(x+iy)=\frac{i}{2}\operatorname{logn}\frac{1+y-ix}{1-y+ix}+2k\pi \quad (37)$$

Example 3

$$\tan^{-1}(-2-i4)=1.675-i0.2006$$

When computing this by equation 37, $k=-1$. Other values, given by adding $2k\pi$, are equally appropriate.

Inverse Hyperbolic Sine $\sinh^{-1}(\pm x+iy)$

Let

$$\sinh^{-1}(x+iy)=u+iv$$
$$x+iy=\sinh(u+iv)$$
$$=i\sin\frac{u+iv}{i}$$
$$y-ix=\sin\frac{u+iv}{i}$$
$$u+iv=i\sin^{-1}(y-ix) \quad (38)$$

By equation 12

$$\sinh^{-1}(\pm x+iy)=\pm(-1)^n\cosh^{-1}\frac{s+t}{2}+i(-1)^n\sin^{-1}\frac{2y}{s+t}+in\pi \quad (39)$$

where n is an integer or 0,
x is positive,
y is positive or negative

$$s=\sqrt{(1+y)^2+x^2}\text{ (positive value)} \quad (40)$$

$$t=\sqrt{(1-y)^2+x^2}\text{ (positive value)} \quad (41)$$

The principal value of $\sin^{-1}$ (between $-\pi/2$ and $\pi/2$) and the positive value of $\cosh^{-1}$ are taken.

Note that if $x=0$ and $y>1$, $s+t=2y$ and if $y<1$, $s+t=2$.

An alternative solution is

$$\sinh^{-1} A = \operatorname{logn}(\pm\sqrt{1+A^2}+A)+i2k\pi \tag{42a}$$

$$\text{or} \quad = -\operatorname{logn}(\pm\sqrt{1+A^2}-A)+i2k\pi \tag{42b}$$

The two solutions of equation 42a indicated by $\pm$ correspond to the two angles θ and $\pi-\theta$ which have the same sine. Logn $(A-\sqrt{A^2+1})$ is a value of $\sinh^{-1} A$ which differs from logn $(A+\sqrt{A^2+1})$ by logn (-1) or $i(2k+1)\pi$. Evidently one at least of the two values is complex, for any given value of A, whether A is real or complex.

The second or alternative form, equation 42b, gives the same results as equation 42a. It should be used when it enables one to avoid computing the numerical difference of two quantities.

Example 4

$\sinh^{-1}(-2-i4)=-2.184-i1.097$

putting $n=0$ in equation 39 or using the + sign in equation 42b.

Also $\sinh^{-1}(-2-i4)=2.184+i4.239$ putting $n=1$ in equation 39 or using the $-$ sign in equation 42a.

The quantity $i\,2k\pi$ may be added to both these solutions.

The results may be checked by computing sinh, which gives $-2-i4$.

Inverse Hyperbolic Cosine $\cosh^{-1}(x+iy)$

Let $\cosh^{-1}(x+iy)=u+iv$

$$x+iy=\cosh(u+iv)=\cos\frac{u+iv}{i}$$

$$u+iv=i\cos^{-1}(x+iy)$$

If y is positive, by equation 28,

$$\cosh^{-1}(x+iy)=\pm\left[\cosh^{-1}\frac{p+q}{2}+i\cos^{-1}\frac{2x}{p+q}+i2k\pi\right] \tag{43}$$

Also, if y in the following equation is positive, by equation 29,

$$\cosh^{-1}(x-iy)=\pm\left[\cosh^{-1}\frac{p+q}{2}-i\cos^{-1}\frac{2x}{p+q}+i2k\pi\right] \tag{44}$$

The quantities p and q are positive and are given by equations 7 and 8. The quantity x is positive or negative.

The positive value of $\cosh^{-1}$ and the principal value of $\cos^{-1}$ are taken.

An alternative solution is

$$\cosh^{-1} A=\pm\operatorname{logn}(A+\sqrt{A^2-1})+i2k\pi \tag{45a}$$

or

$$=\pm\operatorname{logn}(A-\sqrt{A^2-1})+i2k\pi \tag{45b}$$

Equations 45a and 45b give the same results and the one which involves the numerical sum of two quantities should be used in any given case.

Inverse Hyperbolic Tangent $\tanh^{-1}(x+iy)$

Let

$$\tanh^{-1}(x+iy)=u+iv$$

$$=\frac{1}{2}\operatorname{logn}\frac{1+x+iy}{1-x-iy} \tag{46}$$

$$e^{2u}\cos 2v=\frac{1-x^2-y^2}{(1-x)^2+y^2} \tag{47}$$

$$e^{2u}\sin 2v=\frac{2y}{(1-x)^2+y^2} \tag{48}$$

Squaring, adding, and factoring the numerator

$$e^{4u}=\frac{(1+x)^2+y^2}{(1-x)^2+y^2}$$

$$u=\frac{1}{4}\operatorname{logn}\frac{(1+x)^2+y^2}{(1-x)^2+y^2} \tag{49}$$

Dividing equation 33 by equation 32

$$\tan 2v=\frac{2y}{1-x^2-y^2} \tag{50}$$

Let

$$2v=\pi-\tan^{-1}\frac{1+x}{y}-\tan^{-1}\frac{1-x}{y} \tag{51}$$

$$=\pi-\alpha-\beta$$

where the principal values of $\tan^{-1}$ are taken, that is, α and β are between $-\pi/2$ and $\pi/2$.

$$\begin{aligned}\sin(\pi-\alpha-\beta)&=\sin(\alpha+\beta)\\&=(\tan\alpha+\tan\beta)\cos\alpha\cos\beta\\&=(2/y)\cos\alpha\cos\beta\end{aligned}$$

which is the same sign as y, as it should be, from equation 48. Cos α and cos β are positive.

$$\begin{aligned}\cos(\pi-\alpha-\beta)&=-\cos(\alpha+\beta)\\&=(-1+\tan\alpha\tan\beta)\cos\alpha\cos\beta\\&=(1-x^2-y^2)\frac{\cos\alpha\cos\beta}{y^2}\end{aligned}$$

which is the same sign as $1-x^2-y^2$ and proportional to it (see equation 47). The quantity $2k\pi$, where k is an integer, may be added to equation 51.

Therefore

$$\tanh^{-1}(x+iy)=\frac{1}{4}\operatorname{logn}\frac{(1+x)^2+y^2}{(1-x)^2+y^2}+\frac{i}{2}\left\{(2k+1)\pi-\tan^{-1}\frac{1+x}{y}-\tan^{-1}\frac{1-x}{y}\right\} \tag{52}$$

where the principal values of $\tan^{-1}$ are taken and where x and y may be positive or negative (reference 3, page 115, and reference 5, number 715).

An alternative method of computation is to use equation 46 directly. Multiple values will be obtained since expression 23 for logn has a term $i2k\pi$.

Example 6

$\tanh^{-1}(-2+i3)=-0.1469+i1.339$

putting $k=0$ in equation 52

or $=-0.1469+i4.4806$

putting $k=1$.

The choice of the alternative formulas involving logarithms of complex quantities, which have been given in each case in this paper, depends to some extent on the practice and preference of each individual. While the algebraic logarithmic formulas are shorter to write than the others, the solution of the numerical problems in this paper has seemed longer by the logarithmic formulas.

The use of logarithmic formulas for obtaining results involving inverse functions is given in equations 125, page 71 and 428, page 179 of volume 2 of "Communication Networks," by E. A. Guillemin (reference 4).

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A Practical Discussion of Problems in Transformer Differential Protection

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Synopsis: Some conditions which have to be met with in protecting transformers by the usual current-differential method produce special problems. Those involving the protection of interconnected wye or zigzag transformers and those involving Scott-connected transformers are dealt with at some length. There is also a discussion of tolerances in the selection of current transformers and the factors to be considered in the selection of relays for the purpose.

ELECTRIC-power systems are becoming more and more complex insofar as their protection is concerned. Until now there has been little difficulty in obtaining almost anything that one might desire for the usual run of problems, such as the protection of machines, transmission lines, transformers, and so forth. Even systems, which have grown to the point where they have generators, transmission lines, transformers, and other equipment with widely diverse characteristics connected together, have been successfully protected. The war has now changed things, so that today we are having to bring back into service various pieces of apparatus which have been considered obsolete, and which one hoped would remain in that class forever. To connect old apparatus into a well-designed power system and protect them both with equipment not too well-suited to the purpose is a real problem for the engineer responsible for relay protection. Often the circuit is needed in service very quickly, but the delivery dates for relays, current transformers, and so forth, cannot be promised for many months ahead. There is also the question of priorities.

It is quite probable that some old two-phase equipment will have to be rehabilitated and put back into service. For this reason, the question of the protection of Scott-connected transformers by the current-differential method should receive some attention.

While there have been on the market several types of relays and current transformers which, when properly applied, give very satisfactory results, we are now faced with the fact that we must use considerable ingenuity to "get by" with such things as we may have on hand or can procure readily.

A few years ago much time and money were spent on current-differential protections to see that the various component parts of the circuit were built as closely to their theoretical ideals as possible. Current transformers of exact special ratios and with matching characteristics were specially purchased and installed. Current-transformer cable connections were carefully tested so that their resistances might be closely matched. Relays of very special types were considered to be absolute essentials. In fact, there was, generally speaking, considerable superstition in some places about this type of problem.

Today, with our accumulated data based on experience and more accurate knowledge of those conditions which must be satisfied in order to ensure a reliable protection, many of the old ideas have been greatly modified, and we know that we can do things which at one time were considered to be quite hazardous to the continuity of service.

A transformer differential protection is much more likely to operate when it should not do so than not to operate when it should do so. So that, provided the relays are in working order and their current windings are not short-circuited, they will operate if they can get sufficient current at any time. The chief concern with differential relays is to see that they do not respond too readily to transient currents and to be sure that their multiple trip circuits are in an operable state at all times. So sensitive is the usual current-differential protection that a loose connection on one current-transformer wire has been known to operate the relay connected with it.

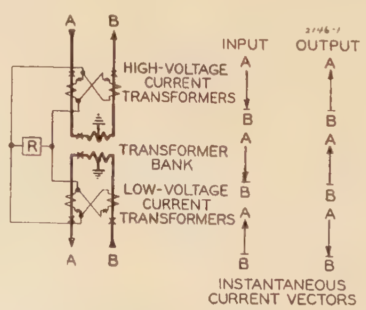

Figure 1. Single-phase transformer differential protection

The chief sources of trouble for which the designer must be on the watch when working up the studies for a differential protection are:

1. The possibility of the generating capacity of the system being so reduced under light load conditions that the fault current available in the protected zone may be insufficient to operate the relays.

2. The possibility that the relays may be so sensitive that they may operate during periods of transient differential currents, which may be produced by the magnetic characteristics of the transformer and of the current transformers, at times of switching, or of heavy faults which draw current through the protected zone but are themselves outside of it.

In the past there was some doubt about the suitability of bushing-type current transformers for use in differential current schemes. It is now accepted that, except in the cases of those with turn ratios too small to be reliable for any kind of use, bushing-type current transformers are as reliable as wound ones for relay work, especially when the relays are required to work, that is, during periods of heavy current flow due to fault conditions.

Fundamental Principles

Let us now review the more important basic conditions which must be satisfied in all reliable current-differential protective schemes for three-phase transformers.

1. Current transformers must be provided in every phase connection to the transformer or transformer bank, so that all current entering and leaving the windings by normal paths may be properly measured and accounted for in the balancing of the differential circuit.

2. Delta-connected transformer windings ordinarily are provided with current transformers with wye-connected secondary windings. They are the most accommodating of all circuits for which to provide differential protection, because, provided proper attention is paid to the ratio of the current transformers, it is immaterial whether their current-transformer secondary circuits be wye-connected or delta-connected.

3. Any transformer windings which are connected in wye or zigzag and are so arranged that current can return to any phase through a neutral connection will at times have some current returning which is not measured by the phase current trans-

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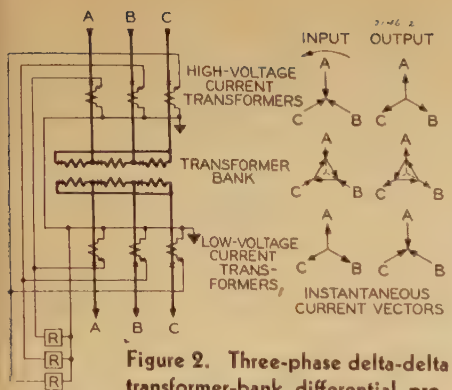

Figure 2. Three-phase delta-delta transformer-bank differential protection

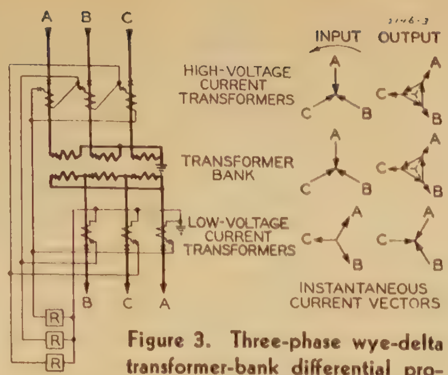

Figure 3. Three-phase wye-delta transformer-bank differential protection

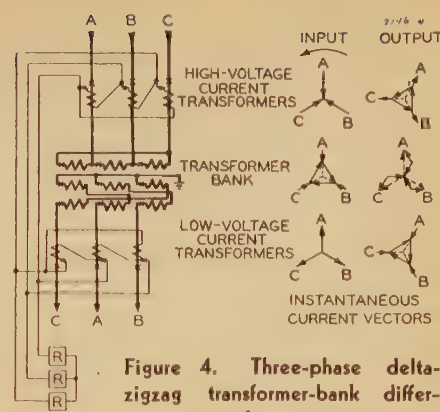

Figure 4. Three-phase delta-zigzag transformer-bank differential protection

formers. This would cause an unbalance in the differential circuit. This unbalance will take the form of zero-phase-sequence components in the current-transformer secondary circuits which measure the currents in the wye-connected or zig-zag-connected transformer windings. Currents which do not sum to zero in delta-connected current-transformer secondary circuits will circulate the difference current around the delta. This circulating current is for all practical purposes the sum of the zero-phase-sequence components and corresponds to the power current returning through the neutral connection. The currents leaving from the delta and entering into the differential circuit will be a true measure of the phase power currents and will balance out satisfactorily. Because of these facts, it is generally accepted that circuits connected to wye- or zigzag connected transformer windings *must* have a delta circulating path for the zero-phase-sequence components in their current-transformer secondary circuits. This is necessary to ensure that neutral currents will not upset the differential current balance and thus operate the relays for some external condition when there is no fault in the differentially protected transformer zone.

4. The instantaneous vector sum of all the currents from the current-transformer leads connected to the relay for any one phase should be as nearly as possible zero at all instants of time when no internal fault exists. For this reason the phase angles between all the currents must be theoretically either zero or 180 degrees, depending upon the relation of the power transformer windings to each other, when no internal fault exists.

5. The current transformers in all connections to the transformer bank must have their performance characteristics so well-matched that transient differential secondary currents may be held to a minimum at all times when no internal fault exists.

Conditions 1, 2, and 3 can usually be arranged without much difficulty. Conditions 4 and 5 are ordinarily speaking never possible to be achieved to within their closest theoretical limits.

The Current Connections

The simplest case of all is that of a single-phase two winding transformer as shown in Figure 1. The three-phase two-winding delta-delta-connected transformer bank shown in Figure 2 is quite commonplace. Another case quite as common is the wye-delta-connected transformer bank shown in Figure 3. These three cases serve to illustrate how conditions 1, 2, and 3 are usually satisfied.

It should be noted that no neutral current or ground fault current can return to the delta-connected windings of a transformer bank, except through the admittance of the windings from the cores to their conductors. This current is always of a very small magnitude and can be ignored for the purposes of studies in current-differential protection.

It should also be noted that by putting wye-connected current transformers on the delta side of the transformer bank and delta-connected current transformers on the wye side of the transformer bank, the phase angles of the secondary currents can quite easily be adjusted so that they will satisfy condition 4.

The case of the delta-zigzag transformer bank shown in Figure 4 is one of the newer ones to come up, and, as will be seen by a study of its vector diagrams, is a hybrid one. The phase relations of the high-voltage and low-voltage circuits are the same as those for a delta-delta-connected transformer bank. There is a path for zero-phase-sequence current through the neutral connection of the zigzag winding; therefore, the current transformers on this side must have their secondary circuits connected in delta. We must not shift the phase relations of the current-transformer secondary currents on the delta side, and so these current transformers must have their secondary windings connected in delta also. This differs from the arrangements shown in Figures 2 and 3.

The three-winding delta-zigzag-wye transformer bank, shown in Figure 5, presents an interesting problem. Let us look at the connections of the windings and from the foregoing discussion decide what the requirements will be for each set of windings of the transformer bank.

1. The high-voltage winding is delta-connected and, therefore, will have current transformers with wye-connected secondary windings.

2. The intermediate-voltage windings are connected in zigzag, and, therefore, the current transformers *must* be delta-connected.

3. The low-voltage windings are wye-connected, and, therefore, the current transformers *must* be delta-connected.

Now the vector diagrams for the transformer bank show that the intermediate-voltage and the low-voltage windings have their output currents 30 degrees out-of-phase with each other; therefore, they cannot ordinarily both have current transformers with delta-connected secondary circuits and at the same time have their phase angles adjusted to a balanced condition. Furthermore, the high-voltage windings and the intermediate-voltage windings have their output currents in-phase with each other, and, therefore, the phase angle must not be disturbed as would be the case with dissimilar current-transformer connections.

Table I

Full Load of Transformer Bank=15,000 Kva 3-Phase

Current-Transformer Ratios Figured for 25 Per Cent Overload. Base 60 Kv

		Current-Transformer Ratios			
Kilovolts	Amperes	Required	Available	Current-Transformer Secondary Amperes	Normal Relay Current Amperes
63	137	172/5	200/5	4.3	—0.2
61.5	141	175/5	200/5	4.38	—0.12
60	144	180/5	200/5	4.5	0
58.5	148	185/5	200/5	4.63	+0.13
57	152	189/5	200/5	4.73	+0.23
12.2	710	888/5	1,000/5*	4.44	—0.06
12	722	902/5	1,000/5*	4.51	+0.01
11.8	734	918/5	1,000/5*	4.59	+0.09
4.32	2,004	4,340/5[g]	4,000/5	5.43	+0.93
4.16	2,082	4,508/5	4,000/5	5.64	+1.14
4	2,165	4,687/5	4,000/5	5.86	+1.36

g Equals $2{,}506 \times \sqrt{3}/5$ to compensate for the delta connection.

* Auxiliary current-transformers 15/5 amperes current step-down to compensate for the delta plus inverted delta connection.

The high-voltage delta-connected windings, having wye-connected current transformers, will provide secondary currents which represent the phase angles of reference to which the secondary currents from the current transformers connected to the other two sets of power windings must be adjusted.

This difficulty can be overcome successfully by the arrangement shown in Figure 5. The current transformers in the leads to the zig-zag windings are connected in delta. This has provided the necessary circulating path for the zero-phase-sequence components, but it has shifted the phase angles of the currents. A set of auxiliary current transformers must now be used to shift the phase angles back again. It should be noted that normally there will be a one to three step-up in current from the secondary currents in the original current transformers to the output of the auxiliary current transformers into the differential-relay circuit. This factor and any other adjustments in ratio that may be necessary can be taken care of by selecting auxiliary current transformers of a suitable ratio.

To determine the ratio required let:

I_p = the current in amperes from the zig-zag windings of the power transformers

I_r = the current in amperes required for the current differential circuit

$R_1/1$ = ratio of the current transformers in the leads from the zigzag windings of the power transformers

$R_2/1$ = ratio of the auxiliary current transformers

Then

$$R_2 = \frac{I_p \cdot (\sqrt{3})^2}{R_1 \cdot I_r}$$

The low-voltage wye-connected windings present no difficulty. The delta-connected current transformers provide the necessary circulating path for any zero-phase-sequence components, while the phase angles of the secondary output currents are adjusted to match those from the high-voltage side.

A more complicated case is the one of the "Scott"-connected transformer bank as shown in Figure 6. It will be noticed that the worst possible condition for current-differential protection has been considered. The high-voltage circuit is three-phase four-wire. The low-voltage circuit is two-phase four-wire and also has the mid-points of the phase windings of the transformers interconnected and grounded. Here are possibilities of three-phase neutral current and also of two-phase neutral current. Obviously, the three-phase current transformers must have their secondary windings delta-connected.

The two-phase circuit presents the real problem. We have to achieve two things:

1. To arrange that the over-all ratio of the current transformers is such that the final output will be of the right magnitude for the current differential circuit.

2. To change these output currents from two-phase to three-phase.

Of course the fundamental idea must be the same as that of the "Scott" connection, but it is not so easily accomplished with current transformers as with power transformers. The transformer producing the current in the phase *W-X* is the teaser of the transformer bank. Therefore, the current transformers in the circuit *W-X* must perform the same function in the current-differential circuit, by producing the quadrature components for *B* and *C* phases as well as providing the current for *A* phase.

Three auxiliary current transformers are required: two of them must have three windings, the other needs only two windings.

The two three-winding current transformers are called phase-mixing transformers for want of a better name. As will be seen, one of them mixes phases *W-X* and *Y-Z* to produce a resultant current for *B* phase. Similarly, the other one mixes phases *W-X* and *Y-Z* to produce a resultant current for *C* phase. The difference between the two resultants is achieved by reversing the direction of the current *Y-Z* in one of the phase-mixing transformers.

The two-phase current transformers have no interconnection between phases *W-X* and *Y-Z*. Therefore, any two-phase neutral current components when passing through the auxiliary current transformers will produce three-phase zero-phase-sequence components in the output circuits.

By connecting the output windings of the auxiliary current transformers in delta, a circulating path is provided for the zero-phase-sequence components, and the phase angles of the output currents are adjusted to match those from the three-phase side.

Let us now get on to the determination of the current transformers required.

Let

I_h = the current in amperes in the three-phase side of the power transformers

I_l = the current in amperes in the two-phase side of the power transformers

$\left.\begin{array}{l} I_{w-x} \\ \text{and} \\ I_{y-z} \end{array}\right\} =$ the currents in amperes in the respective current-transformer secondary windings on the two-phase side

I_r = the current in amperes required for the current-differential circuit

$R_1/1$ = ratio of the current transformers in

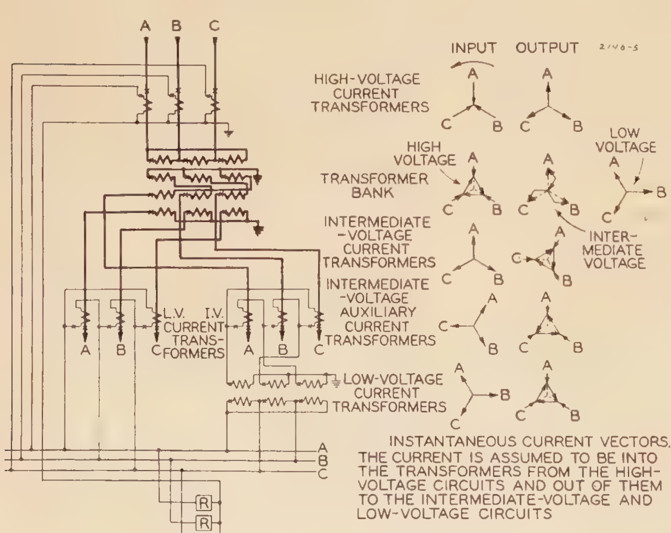

Figure 5. Three-phase delta-zigzag-wye transformer-bank differential protection

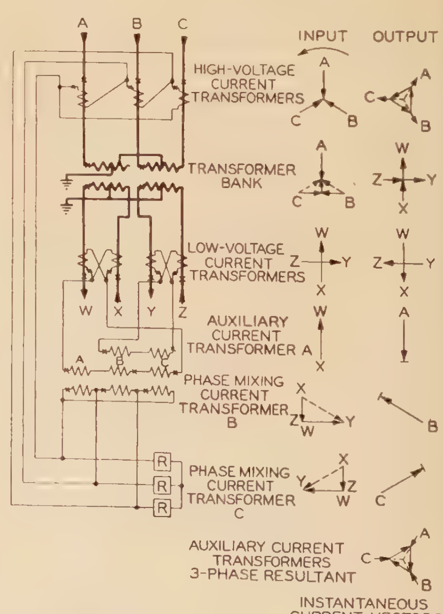

Figure 6. Three-phase to two-phase "Scott"-connected transformer-bank differential protection

Table II

Full Load of Transformer Bank = 5,000 Kva 3-Phase–2-Phase
Current-Transformer Ratios Figured for 25 Per Cent Overload. Base 60 Kv

Kilovolts	Amperes	Phase	Current-Transformer Ratios: Required	Current-Transformer Ratios: Available	Current-Transformer Secondary Amperes	Normal Relay Current Amperes
63	46	3	100/5[g]	100/5	5	−0.2
61.5	47	3	102/5	100/5	5.1	−0.1
60	48	3	104/5	100/5	5.2	0
58.5	49	3	106/5	100/5	5.3	+0.1
57	51	3	110/5	100/5	5.5	+0.3
6.86	364	2	455/5	$\frac{1,000}{2}/5$†	4.55*	−0.65
6.6	379	2	474/5	$\frac{1,000}{2}/5$†	4.74	−0.46
6.34	394	2	493/5	$\frac{1,000}{2}/5$†	4.93	−0.27

g Equals $58 \times \sqrt{3}/5$ to compensate for delta connection.

* Three-phase current the same value after passing through phase-mixing current transformers.

† Two 1,000/5 current transformers with secondary windings in parallel = 500/5.

the three-phase leads of the power transformers

$R_2/1$ = ratio of the current transformers in the two-phase leads of the power transformers

$R_3/1$ = ratio of the auxiliary current transformer for A phase

then

$$R_1 = \frac{I_h \cdot \sqrt{3}}{I_r}$$

$$R_2 = \frac{I_r \cdot 2}{I_{w-x}} \text{ or } \frac{I_r \cdot 2}{I_{y-z}}$$

and

$$R_3 = \frac{I_{w-x} \cdot \sqrt{3}}{I_r}$$

Now when $I_{w-x} = I_{y-z} = I_r$

then the current input for B and C phases equals

$$0.866 I_{y-z} + j0.5 I_{w-x}$$

and the ratios of the phase-mixing transformers when expressed in terms of amperes will be

$$1.154 I_{y-z}\ 2I_{w-x} \Big/ \frac{I_r}{\sqrt{3}}$$

This expression will hold good for any values of I_{w-x}, I_{y-z}, and I_r, even if they are not equal.

Generally speaking, a transformer differential protection should cover not only the transformer bank but as much more of the station equipment as possible. In Figure 7 there is shown an arrangement which can be considered as typical. Here there is shown a three-winding transformer together with its necessary circuit breakers and connections which are all included under the one differential protection. A fault anywhere in the circuits between the transformer and the six groups of current transformers, including the transformer itself, will cause the differential relays to operate. In large stations the bus bars indicated and the circuit breakers connected to them may be separated by considerable distances, so that the connections may include quite long pieces of cable. The differentially protected zone would then reach out to cover equipment situated all over the station installation.

Disturbing Factors

The foregoing discussion of the current-transformer circuits is based on the assumption that all instruments and machines with magnetic cores behave absolutely normally at all times. They do so only after the currents have reached their steady state. Unfortunately, they behave very abnormally during the times that transformers or machines are being cut in and out of service, and also during the times of heavy faults anywhere on the system which can greatly disturb the current conditions in any particular protected zone.

The most usual abnormal conditions are referred to as

1. Through-current transients.
2. Transformer charging-current transients.

Through-current transients are caused by faults which draw very heavy currents through the transformer bank, and the transient magnetic characteristics of the circuit are such that, for a few cycles, the current transformers on one side of the transformer bank do not deliver secondary currents which will balance those from the other side.

Transformer charging-current transients are caused by the inrush of the charging current of the transformer bank at the instant that the first circuit breaker closes. Obviously, the current must appear in the current transformers on one side of the transformer bank only when only one circuit breaker is closed. Fortunately this current persists for only a few cycles also.

Without discussing these disturbing factors in further detail, let us say that the problem that confronts one from both of these conditions is to get the relays to ignore these transient currents and still remain sensitive enough and fast enough to clear any faults in their zone of protection before excessive damage can result from them.

Here again the war conditions enter into the picture. Old transformers often take very high charging inrushes of current. Current transformers of older types often have saturation curves and d-c time constants which differ widely from those which are built today. In fact, many of them were built when little attention was paid to standardization of performance. Therefore, one may be forced by circumstances to make use of current transformers which have characteristics differing very widely one from another. These conditions are almost sure to produce excessive current-transformer transient differential currents.

There are quite a few types of relays on the market which are arranged to be restrained during transient conditions and are designed on very sound theoretical

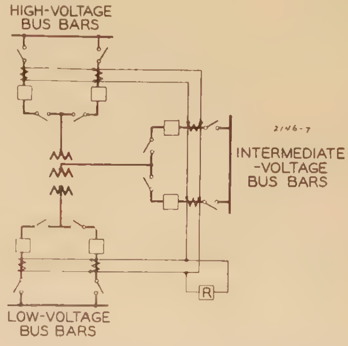

Figure 7. Grouping of current transformers for transformer-bank zone differential protection

Single-phase connections shown

bases for the purpose. Unfortunately, these relays are quite expensive to put into temporary installations. Also it is becoming more and more difficult to obtain relays of ordinary standard types, let alone those which are of quite special types, as these restrained relays are.

Tolerable Differential Currents

There are some compensating factors which help to simplify the problem. It is quite obvious that insofar as the relays are concerned, either the transformer charging current, or the through transient current will produce the greater differential current. Therefore, if we can restrain them for the greater current, we shall have no trouble with the smaller one.

In case of a fault in the protected zone, the damage done will be a function of the time that the fault persists and of the watts that are poured into it. If the impedance to a fault be high, and the power fed into it be therefore low, one may stretch out the time it may be al-

lowed to persist without doing any excessive damage.

It is quite usual in practice to find that current transformers cannot be arranged so that the differential current through the relays is zero at all times. There will almost always be some permanent differential current flowing which will be added to the true differential current under fault conditions. Insofar as the relays are concerned, this permanent differential current is the same as a permanent fault of very high impedance. A transient differential current will be the same as a transient fault, usually of relatively high impedance.

The Relays

Let us assume that one has not much choice of the current transformers and relays that one can obtain for any given differential protection. Let us assume also that, in a hypothetical case, mathematical investigation has yielded the following results for a normal five-ampere relay circuit on a 60-cycle system.

Maximum differential current due to through-current transients	—12 amperes for 20 cycles
Maximum differential current due to transformer charging-current transients	—15 amperes for 15 cycles
Normal 125 per cent load differential current through relays	—2 amperes
Maximum fault differential current under minimum system generating capacity	—5 amperes

If we can obtain both time delay and instantaneous relays of suitable types, we can adequately protect the transformer bank by using one instantaneous and one time relay per phase and set them as follows:

Instantaneous relay:	Pickup at 20 amperes
Time delay relay:	Pickup at 2.5 amperes Operate at 14 amperes in 30 cycles or at 17 amperes in 23 cycles.

The actual setting of the time-delay relay will depend upon its inherent characteristics. It must be determined by test. Let us suppose that for a given relay setting we get the following performance:

Operate at 14 amperes in 30 cycles.
Operate at 17 amperes in 15 cycles.

and for another setting we get

Operate at 14 amperes in 35 cycles.
Operate at 17 amperes in 23 cycles.

Then the second setting should be the one to be selected, because it gives ample time clearance for both through-current transients and transformer charging-current transients. The first setting comes too close to the anticipated time for transformer charging-current transients to persist and might result in false operations.

Almost always the fault current will be high enough to operate the instantaneous relay, but the time-delay relay is necessary to provide against faults at times when the short-circuit current to the fault would be sharply limited by the system setup. Transient currents will not operate the instantaneous relays, because they cannot reach a high enough value. They may, however, start the time-delay relays in operation, but they will die out before the relays can close their contacts. The low value of pickup current of the time-delay relays—0.5 amperes above the normal 125 per cent load differential current—will ensure that they will operate under minimum fault conditions and will not pick up under normal conditions. It is true that the time will be long in clearing, but the limited power available at the fault will limit the extent of the damage that can be done. Most manufacturers of relays now combine these two types of relays in one case.

In some places it is customary to use some sort of additional suppresser circuit to handle transient conditions. The suppresser circuit is usually arranged to shunt away part of the transient differential current from the usual percentage differential type of relays and slow up their action until the transient conditions have passed. This arrangement has one great disadvantage. There is no instant of time when a fault may show up in a transformer bank or its connections in the protected zone more than the one when the first circuit breaker closes and the charging current rushes in. A suppresser circuit makes the relays far less sensitive while it is in operation than they would be normally. Therefore, any fault developing before the suppresser circuit has timed itself out will require a much greater fault current to operate the differential relays than would otherwise be necessary. At times with low generator capacity in service this may cause a complete failure of the differential protection. Some suppresser circuits, which operate on straight voltage for their timing, could never time out during heavy faults in the protected zone, due to the voltage around the zone falling to a very low value, perhaps almost to zero.

On the whole, considerable tolerance can be allowed in the selection of current transformers and relays for current-differential schemes as will be seen from Tables I and II. Table I is worked out for a delta-zigzag-wye transformer bank such as the one shown in Figure 5. Table II is worked out for a "Scott"-connected bank as shown in Figure 6.

While it is always better to have instruments such as relays perform a function for which they have been especially designed, there are often quite a few applications for relays of certain types which were not contemplated at the time the relays were originally thought of. Fortunately, ordinary overcurrent relays can, with care, be made to suffice quite well for transformer differential protection, if time and money are important factors in getting the installation into service.

Conclusions

The most important and, in fact, the essential thing to make sure of in the design of a current-differential protective circuit is that the instantaneous directions of the various currents which must be matched are right and that they cancel out fairly well at the relays. This requirement naturally presupposes that there will be no phase-angle shifts between the currents coming into the relay for any one phase at any time. The exact balance of the magnitudes of the various currents is not very critical. The exact matching of the characteristics and ratios of the current transformers, which are used, is not necessary under ordinary circumstances. It may be necessary where unusually high sensitivity is required. While special types of relays for this work are normally available and give good results, it is not essential that one have anything but some reliable instantaneous and time-delay overcurrent relays in order to provide adequate protection in most cases. Necessary phase-angle shifts of currents, in order to adjust the instantaneous directions of the currents reaching the relays, can be made with auxiliary current transformers. These should have as low an internal burden as possible, in order not to upset the ratios of the power current transformers with which they are connected during heavy current periods. In current transformer circuits connected to transformer windings which have a return path for current through a neutral connection, a delta circulating path for the zero-phase-sequence components in the phase leads must be provided.

Some Air-Blast Circuit-Breaker Installations in Canada

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THIS paper gives a brief report on certain installations of air-blast circuit breakers now in service in Canada, including results of interrupting tests and operating experience.

There are in service in Canada 23 air-blast breakers of 69 kv, 138 kv, and 230 kv of the types illustrated. There are 37 breakers in service on 13 kv and 4 kv. These breakers are in service in ten different stations in various localities.

Figure 2 illustrates an installation of two 230-kv breakers in a station, carrying a load of about 250,000 kva per breaker. Two identical breakers are in service in another city.

Figure 4 shows six 138-kv breakers in a switchyard built for ten air-blast breakers. This switchyard was built in 1934, and 138-kv air-blast breakers have been in service continuously since that time in this installation.

Figure 5 shows three 138-kv air-blast breakers in a city station of 135,000-kva transformer capacity.

In Figure 6a is shown a 138-kv breaker in a downtown city station of 80,000-kva transformer capacity, fed by 120-kv underground cables. This station is built for air-blast breakers exclusively and will have an ultimate installation of 24 15-kv and 36 4-kv breakers.

Figure 7 shows one of two 110-kv breakers at a generating station in the north country (above latitude 48).

Air-blast breakers of 69-kv design are shown in Figure 8, in service in a city station of 40,000-kva transformer capacity. In Figure 11 is shown one of five 69-kv breakers in a terminal station where the short-circuit duty is 1,100,000 kva, this being the recorded interruption on test of one of these breakers in this station.

Air-blast breakers in service on 11-kv generator voltage are shown in Figures 13 and 14. Figure 15 shows a group of seven 4-kv breakers now in service in Western Canada.

Interrupting Tests

The type of 230-kv breaker illustrated has been tested at the station where these breakers are in service, located near the center of three large power systems operating in parallel with a combined generating capacity of about 2,750,000 horsepower. The maximum short-circuit duty at this location is 2,500 amperes at 230 kv or 1,000,000 kva, which is the recorded interruption on test of this breaker as shown in oscillogram, Figure 3.

The breaker is rated at 2,500,000 kva or 6,250 amperes at 230-kv three-phase, an amount of short-circuit current not available for test in Canada. However, this breaker has six interrupting elements in series per pole, and tests have been made on one of these elements interrupting 5,385 amperes with 58.8 kv across the element (see oscillogram Figure 6). This test voltage, 58.8 kv, is about 2.67 times the voltage impressed on each of the elements when interrupting a short-circuit current (assuming line-to-neutral voltage across each pole and equal voltage across each of the six elements in series). If the voltage across one pole is assumed to be 1.5 times line-to-neutral voltage, then the above ratio of 2.67 would become 1.78; that is (under the last assumption) the single element was tested at 1.78 times its rated voltage and interrupted 5,385 amperes which is 86 per cent of 6,250 amperes, its rated current. Multiplying 1.78 by 0.86 gives 1.54; that is, the test is an indication that the breaker would interrupt 6,250 amperes with a voltage across one element 1.54 times the normal (equally divided) voltage, and with a voltage across the same pole of 1.50 times the line-to-neutral voltage. It will be noted that the only extrapolation in the above is from 5,385 amperes test current to 6,250 amperes rated current, that is, an assumption of the same kilovolt-ampere capacity at 6,250 amperes. The assumption of 1.5 times line-to-neutral voltage across one pole is one which has been commonly used, and the assumption of 1.54 times the equally divided voltage across one element is believed to be more conservative than necessary. The oscillogram shows that the arc voltage (six arcs in series) is so low that it could be applied all across one element and be only a small fraction of 22 kv (the equally divided voltage across one element). Hence, if there is any practical inequality of voltages across the six elements, it could only occur after the interruption (and until the opening of the series isolating switch, while the blast of air is still passing between the contacts). The electrostatic field is quite uniform, being determined by the metal roof 30 by 48 inches, and a metal bedplate of the same size, 60 inches below it, supported on insulator stacks, about seven feet above the grounded structure. The insulating structure housing the contacts and arc chutes has a large horizontal cross section. The six sets of contacts occupy identical locations in this structure. Such conditions are conducive to uniform division of potential across the contacts.

Tests on the 69-kv breaker (oscillogram Figure 9) also have a bearing on the interrupting capacity of the 230-kv breaker. The interrupting element in the 69-kv breaker is the same as in the 230-kv breaker, except that the width of the 69-kv contact (and arc chute) is eight inches instead of five inches as in the 230-kv breaker. The 69-kv breaker has interrupted 10,000 amperes on a three-phase short circuit on a 63-kv circuit, which test subjected the single element of the 69-kv breaker to a transient voltage of

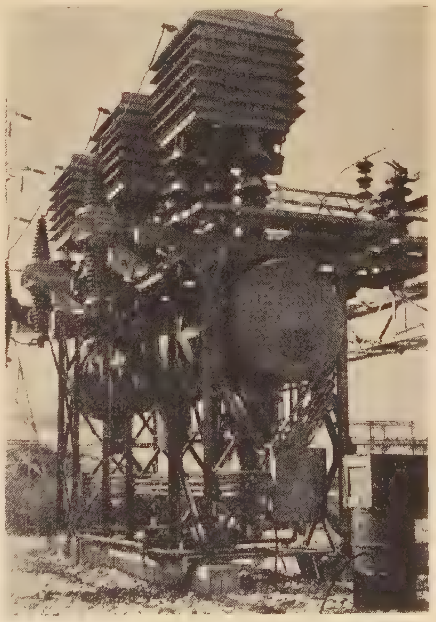

Figure 1. The first 138-kv air-blast breaker of this type

In service from 1934 until 1940

Paper 42-157, recommended by the AIEE committee on protective devices for presentation at the AIEE Pacific Coast convention, Vancouver, British Columbia, Canada, September 9–11, 1942. Manuscript submitted June 3, 1942; made available for printing July 24, 1942.

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Figure 2. Two 230-kv air-blast circuit breakers in service on long 230-kv lines

One breaker carrying approximately 250,000-kva load. Four of these breakers in service in Canada

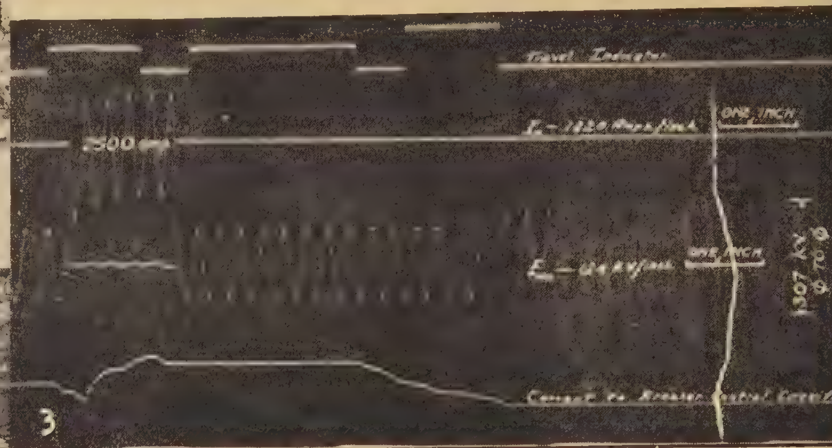


Figure 3. "Make-break" test on 230-kv breaker on 230-kv line, interrupting maximum current available 2,500 amperes

Arcing time one-half cycle (see text for additional tests showing interrupting capacity above 2,500,000 kva)

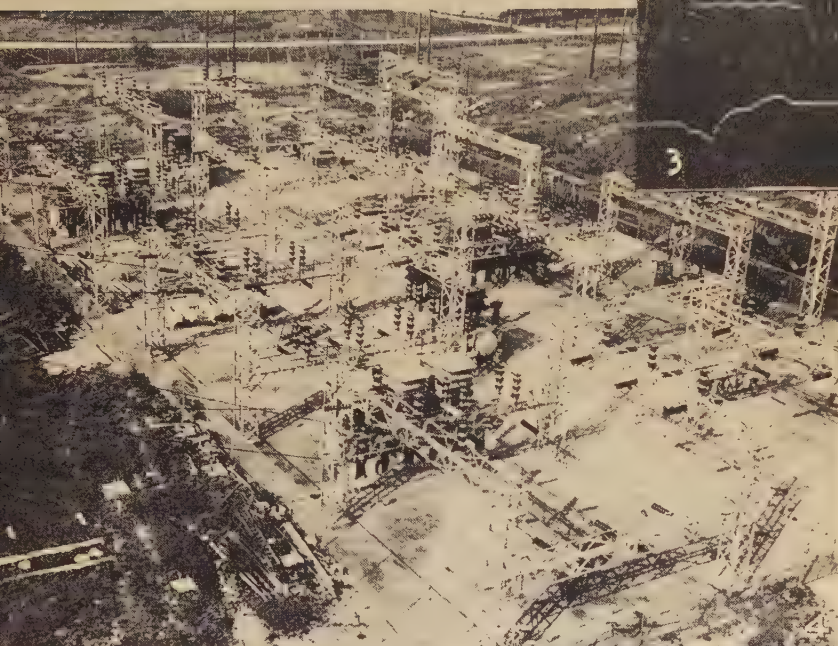


Figure 4. Switchyard built for ten 138-kv air-blast breakers

Six have been installed, controlling 200,000-kw load

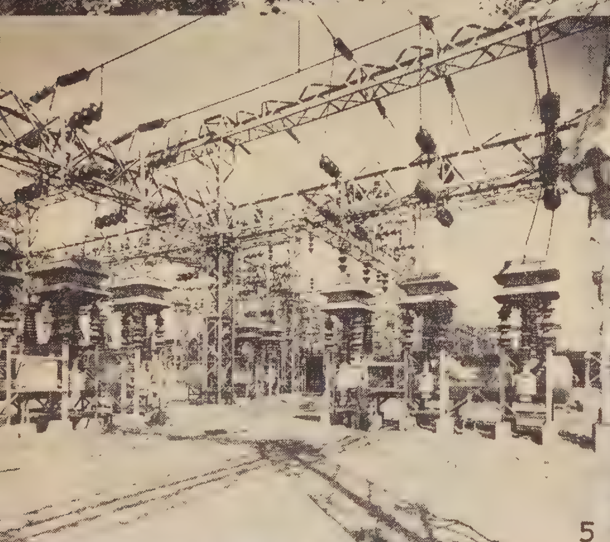


Figure 5. Receiving station switchyard with three 138-kv air-blast breakers installed, operating on 150 pounds per square inch

Closing time 13 cycles—opening time 3 cycles

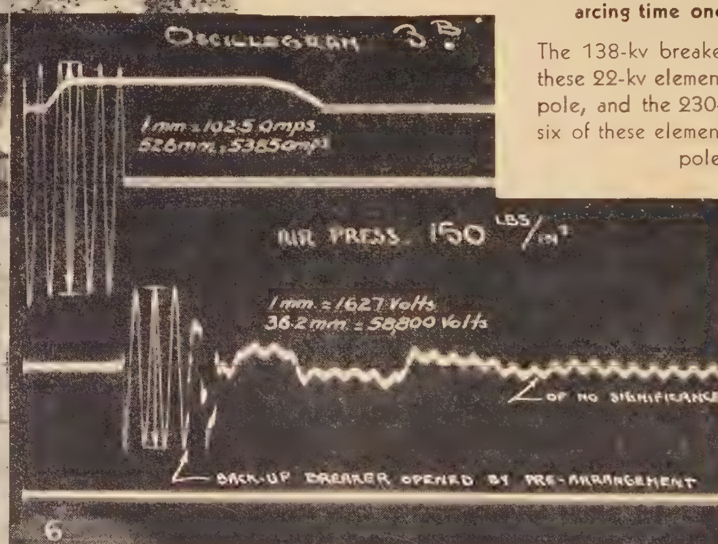


Figure 6. "Make-break" test applying 58.8 kv across one 22-kv interrupting element, interrupting 5,385 amperes, arcing time one-half-cycle

The 138-kv breaker has three of these 22-kv elements in series per pole, and the 230-kv breaker has six of these elements in series per pole

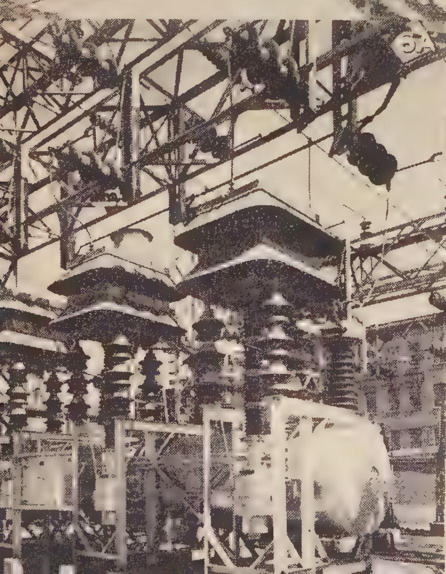


Figure 6a. 138-kv breaker on an underground 120-kv cable-fed substation located in a downtown area

This breaker is within ten feet of a busy street

Figure 7. Two 110-kv breakers at generating station in northern Quebec

These breakers have two interrupting elements in series per pole. Addition of one interrupting element per pole would increase voltage rating to 138 kv and interrupting capacity to 2,000,000 kva

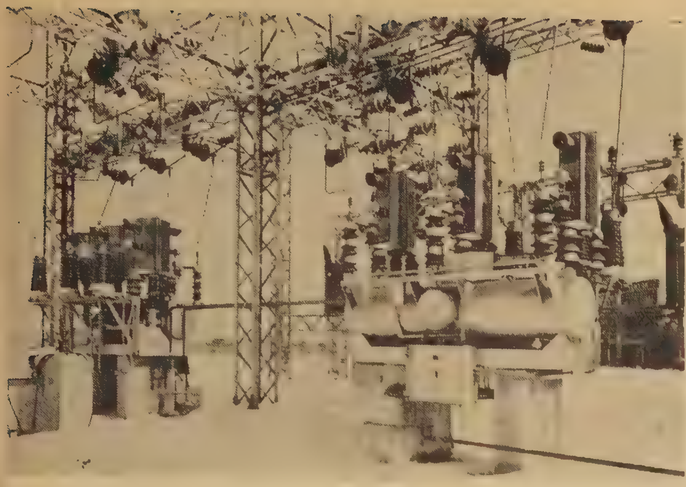


Figure 8. Two 69-kv breakers in city station

Switchyard laid out for nine of these breakers. Each pole has one interrupting element eight inches wide; otherwise the same as the elements in 138-kv and 230-kv breakers, which are five inches wide

36.4 kv. Reducing 36.4 kv in the ratio of five inches to eight inches gives 22.75 kv as the voltage at which the five-inch element should interrupt 10,000 amperes. If the voltages are divided equally in the 230-kv interruption, each element would be subjected to only 22 kv, but assuming 1.5 times line-to-neutral voltage across one pole, then one element would be subjected to 33 kv. Reducing 10,000 amperes interrupted on test, in the ratio of 22.75 kv to 33 kv, gives 6,900 amperes as the interruption at 230 kv, which is 2,750,000 kva. In this case we are interpolating, from the higher test current of 10,000 amperes to the lower current (6,900 amperes) which would justify a slightly higher kilovolt-ampere rating than we have assumed in the voltage ratio calculation.

It will be noted that none of the tests on this style of interrupter showed what is the maximum interrupting capacity. Each of the tests was simply made at the maximum current and voltage available, and none of the tests indicated that the interruption was near the limit of capacity of the breaker.

The 138-kv breakers illustrated have identically the same interrupting elements as the 230-kv breakers, with three elements in series per pole (instead of six). The tests above referred to are, therefore, directly applicable to the 138-kv breaker. The current-interrupting rating is practically the same (6,250 amperes), and the voltage per element is practically the same.

Tests were made also on the 69-kv breaker by applying 58.8 kv across one pole, interrupting 5,580 amperes, which was the maximum current available. The significance of these tests is the liberal overvoltage capacity shown (see oscillogram Figure 10).

In general the tests on all of the breakers above referred to have shown arcing time of one-half cycle. The contacts have shown negligible burning and did not require cleaning up before putting them in service. The arc did not extend outside of the cooling grid. All of the tests were "make-break" and the disconnecting switch contacts making the short circuit performed satisfactorily (the motion of the switch blade being very fast), the arc was not objectionable, and the contacts did not require cleaning up.

The tests were witnessed by disinterested visiting engineers from various power companies, and there was general agreement that no distress was shown in the interruptions.

Operating Experience

Service experience with air-blast breakers in Canada (and the United States) dates from 1934 when the first 138-kv breaker was put in service in the switchyard of a large generating station. It remained in service continuously for about six years interrupting all short circuits satisfactorily. Its isolating switch was very slow, and the workmanship on its interrupting contacts was relatively crude, finally resulting in overheating while carrying load, and it was superseded by breakers of improved design and workmanship. Breakers of the later style have been installed in the same station, and there are now six installed. This first installation demonstrated that there were no serious difficulties in operating and maintaining air-blast breakers of that voltage outdoors in a location subject to climatic extremes of moisture and temperature. The activated alumina twin air dryers installed in that station in 1934 are still in satisfactory operation serving six breakers. The early experience showed the importance of extracting the oil vapor as well as the moisture from the compressed air. There was much improvement made in the detailed technique of determining when the alumina needed reactivating; the color-changing alumina was a practical help, but not essential; the experienced operators got along quite well with the plain white product. The oil vapor (from the lubrication of the compressor pistons) requires a good tight woolen blanket to filter it out. If the blanket does not fit tightly in the container, the oil vapor will blow by. If oil vapor is allowed to pass into the breaker valves (especially the pressure-reducing valve), it will cause the small parts to stick. Consideration has been given to the use of oilless compressors, using carbon piston rings, but the maintenance of the carbon rings and extra cost of such compressors does not appear attractive nor justified. The

Figure 9. Three-phase "make-break" test on 69-kv breaker in 63-kv circuit, interrupting maximum current available 10,000 amperes, equals 1,090,000 kva

Opening time $3^3/_4$ cycles, including one-half-cycle arcing (see text for additional tests indicating higher interrupting capacities)

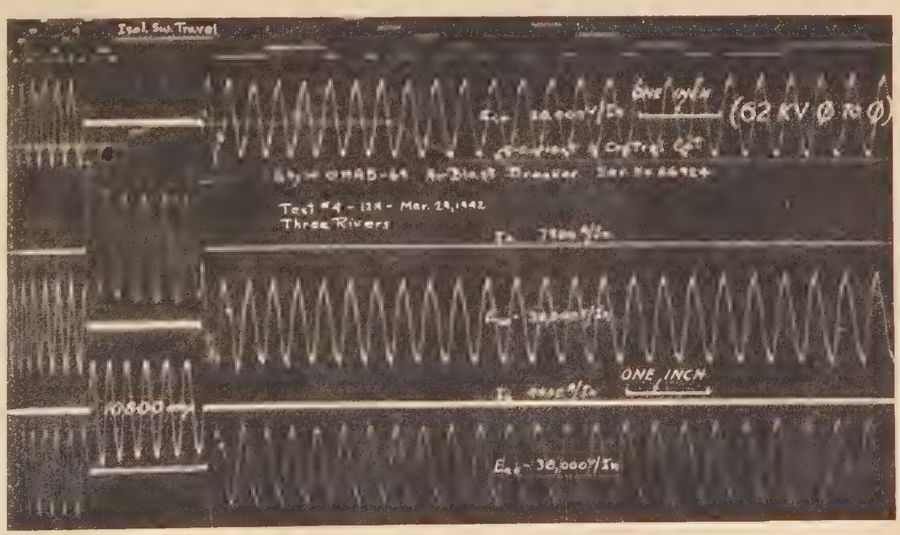


Figure 10. "Make-break" test applying 58.8 kv across one 40-kv interrupting element of 69-kv breaker

Interrupting maximum current available, 5,580 amperes, one-half cycle arcing

Figure 10a. Details of interrupting element

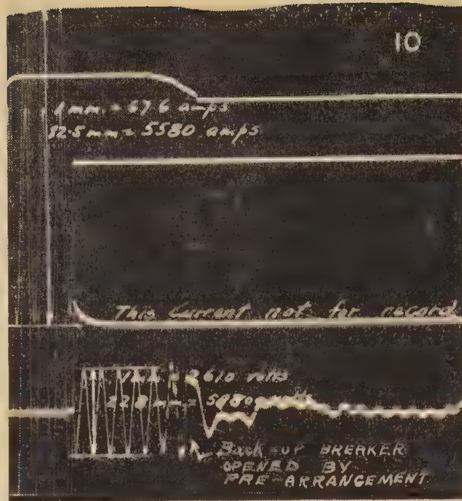
Two elements in series on 110-kv breaker, three elements in series on 138-kv breaker, and six elements in series on 230-kv breaker

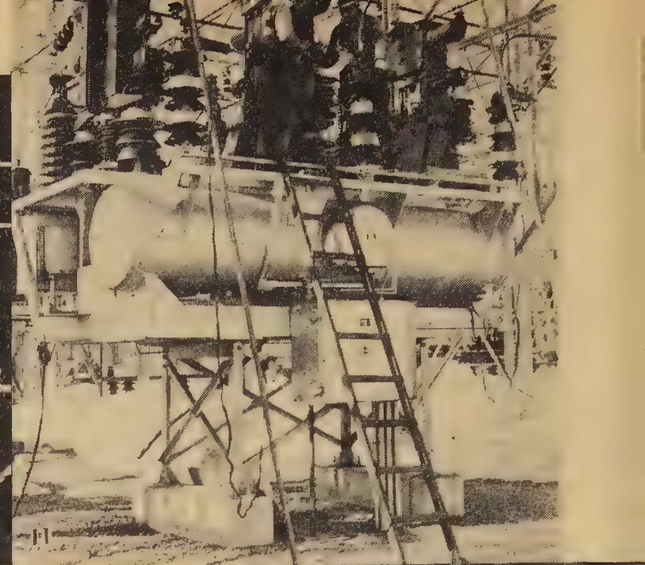
Figure 11. 69-kv breaker 20 minutes after short-circuit test

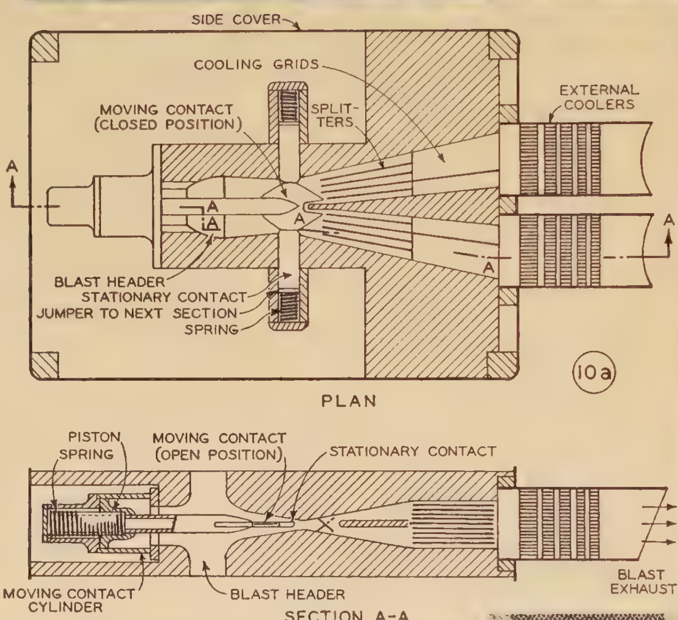
One pole completely taken down, and inspection complete, showing ease of inspection

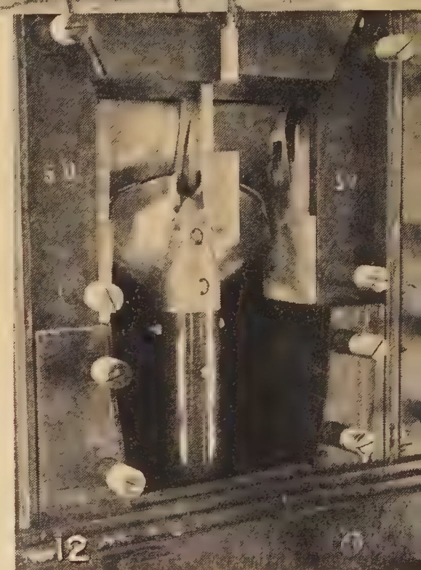
Figure 12. "Inside information" on a 69-kv air-blast breaker after interrupting four short circuits at or above rated capacity

Showing ease of inspection and minimum burning. Moving contact pulled down for inspection









woolen filter has been found quite satisfactory.

There are now in service in Canada 23 air-blast breakers of 69, 138, and 230 kv, in seven different localities. Their service record is equivalent to about 21 "breaker years."

In one instance certain new-style arc-chute details were installed without sufficient interrupting tests, because of the desire to meet promised shipping dates, and certain new parts were found necessary after the tests. In other instances, water had accumulated in the air piping and was not blown out before connecting the breaker, resulting in a flashover in one of the small porcelain "closing" tubes, requiring only replacement of the tube. Aside from such experience, the straight work of installing air-blast breakers does not require any greater skill or labor than installing oil breakers; in fact, the absence of any mechanical linkage between poles of the air-blast breakers eliminates the close mechanical adjustments required in installing oil

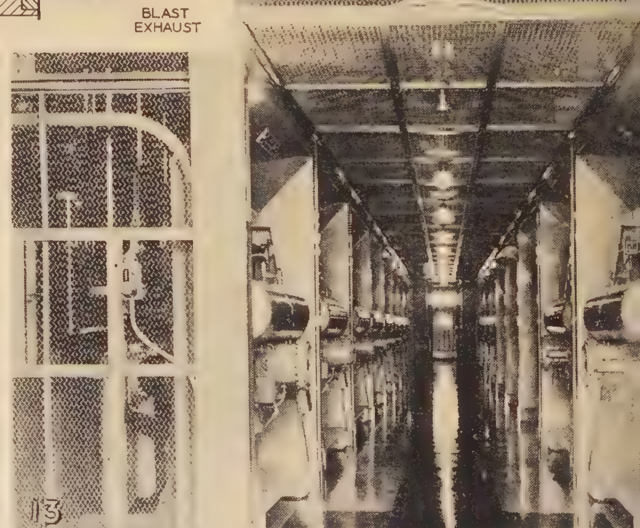


Figure 13. Delle breakers installed in a large power-house

Main generator and transformer breakers operating on 11 kv rated 1,000,000 kva. This station is completely equipped with air-blast breakers

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Figure 14. Factory picture, English Electric Company (Canada) of Delle breakers for four-kilovolt service

These breakers are now in service in western Canada

breakers. Also there is no item in the air-blast job corresponding to the job of filtering large quantities of oil during installation of oil breakers.

In the 69-kv, 138-kv, and 230-kv breakers herein illustrated the contacts and other parts requiring occasional inspection are very accessible. The contacts can be removed and replaced in a small fraction of the time required for corresponding inspection of oil-breaker contacts.

In these breakers the interrupters are supported on standard switch-type insulators (the same as used for bus supports and disconnecting switches). The breakage of the porcelain on such supports does not impair their mechanical strength, and there is no danger of the interrupter falling.

In the experience of several years of testing various styles of interrupter contacts and arc chutes to determine their interrupting limitations, the observers have been impressed with the fact that a failure to interrupt does not result in serious damage. After such a failure to interrupt, the contacts require resurfacing or replacement of arcing tips, certain parts of the arc chute require cleaning up or replacement, but the remainder of the breaker remains intact.

The 138-kv and 230-kv breakers illustrated are readily adaptable to single-pole operation (automatically opening and reclosing one pole only to clear a line-to-ground fault), because there is no mechanical connection between the three poles. The 69-kv breakers have mechanical connection, but this can readily be dispensed with in case single-pole operation is desired.

The opening time (including arcing time) of the 138-kv breaker is within three cycles. The other two breakers take about three quarters of a cycle longer in opening. This can be shortened by about one cycle in the case of the 69-kv breakers by omitting the mechanical tie between the poles. The opening time is also influenced to some extent by the adjustment of certain small valves controlling the opening and closing of the blast valve, so that the opening time of the blast valves and the breaker can be somewhat reduced below the above figures.

Regarding closing time, the breakers as now adjusted take about 12 cycles to close in the case of the 69-kv breaker, about 15 cycles for the 138-kv, and about 19 cycles for the 230-kv. This time is also susceptible of reduction by certain adjustments.

In cases where extremely fast reclosing is desired, this can be arranged by certain adjustments which advance the time of shutting off the blast and applying closing air pressure, it being unnecessary to wait until the isolating switch has completed its opening motion.

It has been noted that all of the tests recorded by the oscillograms were "make-break" interrupting tests. The construction of these breakers is such that the opening and clearing time is the same whether the operation is a "make-break" or a "break" performance, as the breakers are provided with pneumatic tripfree valves.

As shown by the illustrations and remarks concerning the 138-kv breaker, even after one of these breakers has been installed and operated, if changes should be made in the system either by increasing the voltage or increasing the interrupting duty, it is a relatively simple matter to add one or two supporting insulators and one or two interrupting sections, thereby increasing the voltage and interrupting capacity of the breaker without discarding any of the parts. For instance, a system might initially operate at 110 kv and in later years be raised to 138 kv or 180 kv by the additions to the breaker as indicated.

The same remarks apply to the 230-kv breaker in regard to a possible increase in voltage from 230 kv to 280 kv for instance, which could be accomplished by adding one or two supporting insulators.

Low-Voltage Air-Blast Breakers

Figures 13, 14 and 15 show installations of 15-kv and 4-kv air-blast breakers in Canada. The first few breakers of this type were imported from the Delle Company in France. The other breakers of the same design were built by the English Electric Company of Canada. The interrupting capacity has been established by the tests made in the testing laboratories in France and by certain tests made in the testing laboratories of the I-T-E Circuit Breaker Company in Philadelphia. Further tests have been made on these breakers at the stations where they are installed. Certain improvements in detail have also been made by the Canadian company to meet Canadian requirements.

The 15-kv breakers illustrated have been operating satisfactorily for about two years on 11 kv in a large generating station which is equipped with air-blast breakers exclusively.

References

1. Mechanical Simplicity of Air-Blast Breakers, **H. W. Haberl, Otto Jensen.** AIEE Transactions, volume 60, 1940, September section, page 869.

2. Discussion by **H. W. Haberl** of Circuit Interruption by Air Blast. AIEE Transactions, volume 59, 1940, page 1129.

History of A-C Wave Form, Its Determination and Standardization

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FELLOW AIEE

Synopsis: With the birth of the transformer and the first distribution of alternating currents, wave form assumed interest, and methods were developed for its determination, chiefly the point-by-point method of instantaneous contact, mechanical oscillograph, cathode-ray oscillograph, and the oscilloscope with stabilized time axis. The point-by-point method, by which were made the first major contributions, is now practically superseded by oscillograph and oscilloscope, each finding increasing use in its field.

With the determination of wave form accomplished, demand arose for its standardization corresponding to expanding applications. No single standard being suited to all applications, different standards have been developed in different fields, as in power, communication, and insulation. While it is desirable that standards, once set up, remain fairly stable, they should be subject to review and occasional change to keep in step with technological advances. Minor revision in communication is in progress. Although standards in other fields do not appear ideal, no immediate revision is recommended. Forty references are appended.

Alternating Current in the Late 80's

THE distribution of alternating current as we now know it began in 1885–86. Previous to that time, alternators were used to operate arc lamps in lighthouses, each machine operating one large arc lamp, but there was no distribution. Late in 1885, William Stanley made his first constant potential transformer, and early in 1886, using six transformers for operation in parallel distributed current at Great Barrington, Mass., to a score of customers at a distance of 4,000 feet. The secondary voltage was 100 volts. Current was supplied from a Siemens alternator, designed for 12 amperes 500 volts, imported for the purpose. At the time of this first installation, it was pointed out by electricians that "if a high potential primary circuit of 500 volts or more were used to distribute electricity throughout a community, there was a grave fire and life danger." A few years later a bill was introduced[1] but not passed in the Virginia legislature to limit a-c pressure to 200 volts, alternating current being considered (by those opposing its use) to be more deadly than direct.

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The author is indebted to A. C. Crehore, his first co-worker, and to later co-workers to some of whom reference is made; to L. F. Blume of Pittsfield, O. E. Buckley and H. S. Osborne of New York, L. W. Chubb and R. D. Evans of Pittsburgh, who furnished material and references; and to the staff of the California Institute of Technology.

Stanley's installation at Great Barrington was put in regular service in March 1886, and operated until summer, when an attendant dropped a screw driver into the alternator and ruined it. Meanwhile, on April 6, 1886, George Westinghouse, accompanied by W. L. Church, F. L. Pope, W. C. Kerr, and others, had seen the system in operation and determined to actively enter the a-c field. The manufacture of a new type of alternator designed by Stanley, the radial-pole type thereafter generally used, was undertaken in Pittsburgh. The story is well told by Stanley[2] himself with interesting side lights with credit to others.

The following winter the first commercial installation was made at the station of the Buffalo Electric Company. The question of the determination of a-c wave form then arose as an engineering problem. Before considering wave form, we should note the meagerness of knowledge then possessed concerning the behavior of alternating currents.

In the late 80's many misconceptions impeded progress. Series *versus* parallel operation of transformers was much discussed without adequate understanding. In 1886, at the very time that the parallel operation of transformers was successfully accomplished as we have just seen, a distinguished cantor lecturer held such a connection to be impracticable, maintaining that a separate lead to each transformer would be necessary, or each transformer should have a special regulating apparatus.

Referring to constant current operation with transformer primary windings in series, the chief of an electrotechnical testing station explained:[3] "When no secondary current is flowing, the electromotive force in the primary and secondary coils is a maximum. (Quite correct.) We have consequently this disproportion that *the smaller the output of the apparatus the greater the energy consumed.* With the secondary circuit open, and a constant exciting current, the energy used could be as much as ten times as great as under full load." Power factor was overlooked or unknown.

The proponents of the series connection of transformers operated with constant primary current reversed the argument with the same misconception. They held that inasmuch as primary electromotive force decreased with increase of secondary load, power input also decreased (primary current being constant), with the happy result that *as more power was taken out, less power was put in*—surely a condition to be desired. Here again, power factor was neglected or unknown. Yet, without meters for measuring power, power factor or phase, such misconceptions should be expected; they merely reflected the state of the art at the time.

The United States Patent Office had shown a like misconception when in 1883 it refused[4] to grant a patent on a transformer on the ground that it would be impossible to get a larger current out of the secondary winding than was supplied to the primary winding, but in 1886 a patent was allowed for this very thing.

In the Early 90's

Many of the misconceptions of the 80's trailed along into the early 90's. In the field of alternating currents there was a growing collection of isolated facts, some understood and others not, but there was no broad foundation on which to stand. This was the situation, as recalled by the author in 1890 and 1891, when he first became interested in alternating currents and in wave form. The open-magnetic-circuit transformer was still discussed. The merits of the "nonpolar" transformer with its closed magnetic circuit was not yet fully recognized. The capacity "effect" with current before the electromotive force that produced it (the effect before the cause) and the Ferranti "effect," with electromotive force received at the far end of a cable greater than the electromotive force applied, were found baffling.

Two "systems" of a-c distribution were then in use, apparently well-established: the "high-voltage" *constant potential* (Westinghouse) system operating transformers with primary circuits of 1,000 volts and 2,000 volts, with secondary circuits wound for 50 volts and 100 volts for supplying incandescent lamps in parallel;

and the *constant current* system, about 10 or 15 amperes for supplying arc lamps in series. Transformers were limited to small sizes, the dictum from high sources that transformers larger than two kilowatts would not be economical being generally accepted. Higher voltages and the use of a-c motors were in the offing. The attempted use of iron wire for a-c transmission has been reported.

It was in 1890–91 that P. N. and L. L. Nunn installed the first commercial transmission of alternating current for power, to operate a 3,000-volt synchronous motor at the Gold King Mine at an altitude of 11,500 feet near Telluride, Colo., from water power 3,000 feet lower at a distance of three miles. The situation at that time was vividly described by P. N. Nunn in an address (mimeographed) before AIEE Los Angeles section, May 8, 1934, on "Early Experiences in the Power Industry," from which the following is quoted:

"In spite of its shortcomings, alternating current seemed the most feasible. A pair of conventional alternators were installed, one as generator, the other as motor, identical to assure identical 'wave forms,' *whatever that might be.*

"In 1890 alternating current was just plain *freak;* it did not follow Ohm's law and 'clogged' itself in its circuits. Bedell and Crehore had doped out its laws and demonstrated their concepts in 100 pages of solid calculus.

"Wattmeters had not been developed, nor had the term 'power factor' been adopted into the vernacular."

Evidently wave form, power factor, and the clogging effect of impedance were dimly discernible. The need of a standard wave form, "whatever that might be," was thus early recognized. From this first power transmission it was a far cry to the 287,500-volt transmission with its magnificent equipment at Boulder Dam today. The increase from 3,000 volts came slowly with gradual increases in demand for more and more power from greater distances, with notable jumps to 33,000, 40,000, and 60,000 volts. With high voltages and long distances arose problems of insulation and interference, in which questions of wave form play such important part.

Going back to horse-and-buggy days, with constant-current and constant-potential systems both in use, the question arose as to whether incandescent lamps and arc lamps could be supplied from the same system. Early in 1891 a promoter, an ardent believer in the future of the constant-current system,* sought the aid of A. C. Crehore and the author in the development of a constant-current to constant-potential transformer for operating incandescent lamps from such a system. Easier said than done, but with the enthusiasm of youth, the problem was tackled. In this connection the development of the principles governing the flow of alternating currents was undertaken, while at the same time experiments were started on a high-voltage constant-current arc-light circuit, the only supply available.

From the experimental work much was learned, although it was never completed. As at Great Barrington it was learned that a short circuit by a screw-driver could wreck a constant-potential generator; so the lesson was now learned that an open circuit caused by the slipping of a connector from a mercury cup could wreck a constant-current system and cause a general blackout! A mercury-cup contact is ill-suited for high-voltage experiment. Permission for experiment was withdrawn. The work, thus summarily ended, was never renewed; meanwhile a better solution had been found by Elihu Thomson—the "tub" transformer with floating secondary winding delivering constant current from a constant-potential primary winding. Constant-potential primary distribution had become firmly established.

The theoretical work, on the other hand, the development of the principles governing the flow of alternating current, gave better results, opening a fruitful and ever widening field with direct bearing on a-c wave form. This work, prepared initially without thought of publication, formed the basis for the first paper by Bedell and Crehore[5] presented before the Institute at its annual convention (then general meeting) in Chicago just 50 years ago. In this the principles governing current flow in transient as well as in steady conditions were first fully developed. The sequel published in book[6] form later in the same year defined the limitation to telephony (page 201) due to change in wave form of a complex wave along a line[6a] with distributed capacity and the modification produced therein by self-induction, successfully accomplished later by M. I. Pupin with the use of loading coils. In it were included the development of vector methods for solving a-c problems and the extensive use of circle diagrams, now so common.

The first use was here[5,6] made of $j=\sqrt{-1}$ in a-c analysis. Its use in astronomy and other fields had long been known, the symbol $\sqrt{-1}$ as a sign of perpendicularity appearing in a memoir to the Royal Society of Arts and Letters of Denmark by Caspar Wessel[7] in 1797. It is to A. E. Kennelly,[8] however, that credit[9] should be given for bringing out its full significance in the application of complex quantities to a-c problems, and to C. P. Steinmetz for so ably extending its usefulness.

Vector methods and circle diagrams for solving a-c problems were not infrequently criticized, when they were first developed, as being dependent upon the so-called "sine assumption," whereas in fact electromotive forces produced by a-c generators *are not true sine waves.* To this criticism with its part truth there was no categorical answer. As a circle is defined by three points, a circle diagram permits the determination or predetermination of the performance of a-c machines and systems with a minimum amount of observation and calculation. Experimental diagrams[10] were early found to check** closely with theory. The wide use of the circle diagram today is evidence of its value.

The assumption of equivalent sine waves has likewise proved adequate[11] for many cases. For other cases more elaborate methods,[12] sometimes involving vectors in more than two dimensions, have been developed. These methods, also, have been criticized on the ground that electromotive forces produced by most a-c generators *are practically sine waves;* so that these more elaborate methods are unnecessary. Again a part truth! Both assumptions, sine wave and nonsine wave are open to criticism, but each has its field of usefulness. What allowable limits should be set to the departure from a sine wave remains to be determined.

Wave-Form Determination

H. J. Ryan[13] in 1889 made an extensive study of wave forms of a closed-magnetic-circuit transformer under different operating conditions, using a synchronous contact maker to obtain instantaneous readings point by point. In this way complete wave forms of currents and voltages and their phase relations were obtained, showing definitely the behavior of such a transformer. By this method studies were made of generator wave form by Tobey and Walbridge[14] and the wave

* Well into the present century engineers of eminence believed that constant current would be the ultimate in power transmission.

** It is interesting to note that such a close check, even though not complete, could be made without wattmeter, phase, or power-factor meter and with limited instruments for current and voltage measurements. Primary and secondary voltages were determined by reading, with telescope and scale, the elongation of two fine German-silver wires; primary and secondary currents, similarly, with two coarse wires. Very competent were the three observers named in the reference, with later careers of note.

forms of an open-magnetic-circuit transformer by Bedell, Miller, and Wagner,[15] using a liquid-jet contact maker, with some modifications in methods of measurements and the use of capacitors to improve power factor. The open-magnetic-circuit transformer with its large magnetizing current and low power factor could not survive.

The use of a synchronous commutator[16] in place of a contact maker eliminated error caused by duration of contact and made possible the direct determination of flux. The mechanical plotting of points on a synchronous drum was introduced by E. B. Rosa[17] to eliminate tedious plotting by hand. All methods employing synchronously driven mechanism, however, had their day. Besides being cumbersome and inconvenient, they were limited to commercial frequencies and at best gave only points rather than continuous curves.

Meanwhile, a parallel development, starting with the optical study of the excursions of a telephone diaphragm, had led to the oscillograph,[17a] perfected by Blondel, Duddell, and others, employing a suspended element light enough to follow closely the rapid changes in a quantity under observation. There is no need for expanding on the wide and continued usefulness of the oscillograph in the determination of wave form. Point-by-point methods were thus outmoded. The moving element of an oscillograph, however light it may be, has *some* inertia which, though practically negligible for many purposes, limits its ability to follow very rapid changes in the quantity under observation. A vibrator with no weight at all would be most desirable.

It had long been known that a cathode-ray beam would be deflected by a magnetic or electric field. Ryan[18] grasped at this fact and, with a special cathode-ray tube made for him by Mueller Uri, constructed and used the first cathode-ray oscillograph, an oscillograph in which the moving "part" had no weight and could accurately follow the changes in whatever quantity was under observation. A new field was thus opened.

In the cathode-ray oscillograph the spot of light caused by the cathode ray impinging on a fluorescent screen became a curve, by persistence of vision, when the cathode-ray beam was deflected simultaneously by two fields at right angles, one field being set up by the variable under observation, and the other by a known variable of reference. Various Lissajous figures were thus produced. In studying a-c wave form, Ryan used a known sine wave for reference, the resultant curve being a smooth ellipse in case the wave under observation was also a true sine wave. Departure from an ellipse indicated departure of the unknown a-c wave from a sine wave. The observed curve was then laboriously replotted with time as an axis, as there was then no means for obtaining this directly.

The apparatus used by Ryan was cumbersome. The tube required 5,000–10,000 volts accelerating potential, obtained from a motor-driven Wimshurst machine. Furthermore, it required the maintenance of low vacuum, obtained from elaborate vacuum apparatus with frequent attention. These inconveniences were overcome in the low-voltage hot-cathode tube of Johnson,[19] with an accelerating potential of only 300–500 volts and greater sensitivity. The tube contained a small amount of gas and required no re-evacuation during its life. The general use of the cathode-ray tube for determining wave form thus became possible.

To obtain a linear time axis, as now commonly used, required a saw-toothed wave for the wave of reference, instead of the sine wave used by Ryan. Various means for developing such a wave were advanced, some mechanical, as from a synchronously driven rheostat, and some electrical from various types of circuit. Mechanical means were cumbersome and limited in frequency range and never came into general use. Electrical means for obtaining a saw-toothed wave, properly synchronized and stabilized, led to the oscilloscope[20,21] with linear time axis so widely used today.[22,23] To maintain curves stationary, a stable linear sweep circuit is essential. With the oscilloscope practically no energy is drawn from the circuit under test.

With wave form determined, many methods and machines have been developed for its analysis, when plotted either in rectangular or polar[24] co-ordinates, into its harmonic components. Early attempts were made to determine the separate harmonic components in an a-c wave by direct electrical measurement. Some success was obtained by resonance[25] and by other means, as by passing currents of various harmonic frequency through one coil of a split dynamometer, the current to be analyzed through the other. The results, however, were meager, as the harmonic components without amplification, were too small to give significant measurement. Amplification, however, has made possible the development of many successful analyzers that give the components of an a-c wave directly by electrical measurement.[26–29]

Wave-Form Standardization

With the expanding use of alternating currents, the need for the standardization of wave form arose, and the adoption of some factor or factors that would indicate quantitively the degree of departure from a sine wave. Form factor, the ratio of effective to average value, although useful in connection with transformer loss, had no general significance, widely different wave shapes having the same form factor.[30] Other factors were from time to time proposed for this purpose, including distortion factor, peak factor, harmonic factor, curve factor, and deviation, and in some cases, after discussion, were sanctioned by the standards committee. Each factor had its own[31] significance as the numerical measure of the departure of an irregular wave from a pure sine wave, varying each in its own way with variation of amplitudes, phase, and frequencies of the harmonic components of the wave. Each, therefore, had special usefulness for special purposes. Whether a single factor could be found, sufficiently satisfactory for all purposes, was a question.

It was generally agreed that a sine wave of electromotive force at generator terminals or on a transmission line is best for most purposes, and that methods for prescribing allowable departure therefrom should be determined. In 1915 the standards committee, through a subcommittee* on wave form, undertook a study of the subject to ascertain what standard or standards could be specified that would be most suitable in characteristics and practical in application, avoiding tedious analysis and cut-and-try methods as far as possible.

Admittance Standards

As the troubles caused by a departure from a sine wave depend in many cases upon the frequency of the harmonic or harmonics present, the assignment of penalties to different harmonics according to their frequencies appeared to be an obvious way to make the penalty fit the crime. For doing this, an admittance type of wave-form standard[32] appeared well suited, with the possibility of assigning different allowable weights or penalties to harmonics of different frequencies according to their behavior or misbehavior. The admittance of a circuit is readily measured, being proportional to

* Membership of the subcommittee: F. Bedell, chairman; L. W. Chubb, F. M. Farmer, H. S. Osborne, and L. T. Robinson.

current. The admittance of a circuit with capacitance (C) increases, and in a circuit with inductance (L) decreases, with the frequencies of any harmonics in the applied electromotive force. With L and C both in the circuit, the admittance reaches a resonant peak at a particular frequency, the broadness of resonance being controllable by the resistance (R). Values of L and C can, accordingly, be selected for resonance at a particular resonant frequency, giving maximum penalty to a harmonic of that frequency, the admittance and penalty tapering off on each side, more or less rapidly, according to the value of R.

The shape of the tapering slopes on the two sides can be controlled to a certain extent by employing a composite, instead of a simple circuit, with R, L, and C in the admittance standard. Desirable penalties can thus be assigned to different harmonic frequencies according to the degree of crime. The possibility of better weighting thus obtained led to the development of a composite circuit, instead of a simple circuit as an admittance standard, despite the advantage that the latter could be readily duplicated with common laboratory equipment.

The degree of crime, however, and hence the penalty to be assigned, is different in different fields of application, as in power transmission and machinery, in communication or insulation. It soon developed that penalties could not be uniformly prescribed in all fields, and no one universal wave-form standard, however desirable on account of simplicity, would prove generally acceptable. Special standards thus appeared to be necessary to meet practical conditions in each case.

In Inductive Co-ordination

The admittance type of wave-form standard was found to be particularly suitable in the inductive co-ordination field. After extensive studies of induction problems involving power and telephone systems, a *telephone interference factor*, TIF, was proposed[33] in 1919 and a TIF meter, of the admittance type, for measuring power-system wave shape in terms of its influence on telephone circuit noise. Definite weightings were determined, based on the interfering effects of different frequencies, depending in part on the telephonic equipment in use and in part on the characteristics of the human ear. In 1935 changes in telephonic equipment and new studies led to new weightings,[34] and the name telephone interference factor was changed to telephone influence factor, as more appropriate. In 1941 weightings[35] were again revised, with corresponding changes in the admittance net work of the TIF meter. Wave-form standards as applied to inductive co-ordination problems are thus being well cared for.

In the Power Field

In the power field, deviation and distortion factors are both in use, but not extensively. Deviation is included in some specifications, and acceptance tests are made to see that the specification is met. Distortion factor* is more rarely encountered, being sometimes used by designers for calculating performance of machines. Good wave shape is a matter of evolution, attained by experience. With the increase in size of machines good design for wave form becomes less difficult.

By American Standards Association[36] definition, 1.217–10.95.420:

"The *deviation factor* of a wave is the ratio of the maximum difference between corresponding ordinates of the wave and of the equivalent sine wave to the maximum ordinate of the equivalent sine wave when the waves are superposed in such a way as to make this maximum difference as small as possible."

"The deviation factor of the open-circuit terminal voltage wave of synchronous machines shall not exceed ten per cent unless otherwise specified." (Rule 3.220)

By ASA definition, 1.218–10.95.430:

"The *distortion factor* of a voltage wave is the ratio of the effective value of the residue after the elimination of the fundamental to the effective value of the original wave."

To determine deviation, as defined, requires a curve of wave form and the use of cut-and-try methods. Deviation is not directly measurable. In contrast with the admittance type of standard, it takes no account of the frequencies of the harmonic components, a characteristic which may be an advantage or a disadvantage according to the use that is made of it. If there were simple means for its determination, deviation would serve well as a general standard, but no such means are available.

Distortion factor, also, takes no recognition of frequencies, although giving total harmonic content. It can, however, be determined by analytical processes, and indirectly from measurement without requiring a curve of wave form, advantages in its favor.

In Dielectric Tests

In dielectric tests, by ASA rule 2.122:

"The wave shape of the test voltage shall be of acceptable commercial standards: that is, it shall come within the deviation specified as allowable in paragraph 3.220. The test shall be made with a voltage having a crest equal to $\sqrt{2}$ times the test voltage specified."

More recent AIEE Standards[37] specify further in paragraph 3 of appendix:

"With the test specimen in circuit, the crest factor (ratio of maximum to mean effective) of the test voltage shall not differ by more than five per cent from that of a sinusoidal wave *over the upper half of the voltage range.*"

Crest voltages and crest factor may be determined by the use of a synchronous commutator[38] or rectifying tube, specification for a crest voltmeter being given in paragraph 4-80, AIEE Standards.[37]

In a Standards subcommittee report,[39] the use of crest deviation factor (the departure of the crest factor from 1.414 calculated in percentage) is suggested, and it is recommended that, as deviation requires a trace of wave form, it be not used for specifying wave form in dielectric power-factor measurements, and that distortion factor be adopted in its stead.

Summary

Since 1890, when alternating current was considered just plain "freak," not obeying Ohm's law and "clogging itself in its circuits," our knowledge (with or without calculus) has become greatly advanced and the importance of wave form, "whatever that may be," so well recognized as to require most careful standardization.

A fuller discussion of wave-form standardization would here be out of place. Standards must always be subject to revision with technological development, such revision being made only after a strong need has developed. This is not the occasion to suggest revision.

The author has outlined the history of wave form, without venturing a prediction as to the future. The paper merely runs a thread through a maze of material with no pretention to completeness, the story being more fully told in the references and their extensive bibliographies.

* Distortion factor is frequently used in the communication field in rating high-quality program and broadcasting equipment.

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Design and Operation of High-Voltage Axial Air-Blast Circuit Breakers

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Synopsis: The air-blast high-voltage circuit breaker has been developed and applied on high-voltage systems of 150 and 240 kv. Both single-phase and three-phase automatic high-speed reclosing can be carried out with this new type of circuit breaker. The principles of design and operation are explained and the construction of two high-voltage air-blast circuit breakers rated at 150 kv and 220 kv, installed in Canadian power-distribution plants, is described and supplemented by a discussion of performance tests and oscillograms. The construction of an air-blast circuit breaker with axial blast, designed for extremely high voltage and high interrupting capacity, is explained, and results of various tests discussed.

THE air-blast type of circuit breaker was developed very rapidly from the indoor low-voltage type to the outdoor high-voltage type of circuit breaker, as extensive test experience clearly showed the many distinct advantages of applying compressed air for arc interruption. Besides the elimination of the oil hazard, the application of compressed air assures a very fast arc-interrupting ability and enables extremely fast tripping and automatic high-speed reclosing, by means of which the ideal protection of high-voltage power-transmission lines can be achieved.

The description of the following high-voltage air-blast circuit-breaker installations will show to what extent this type of breaker has been developed.

Construction and Operation of 150-Kv Air-Blast Circuit Breaker

The construction of the 150-kv air-blast breaker is illustrated in Figure 1. The breaker consists of three individual poles which are pneumatically interconnected or controlled by a common closing and tripping shaft. Each pole comprises a compressed-air tank (1), with a control valve and piston assembly (2, 3, 4), and a porcelain insulator column (5), on which is mounted the arcing chamber (6), which contains two arc breaks (7), and an air-saving piston arrangement (8). A movable isolating contact (9) is connected in series with the arcing contacts and is operated by a rotating insulator (10). The stationary isolating contact (11) is supported on an insulator (12) which can be designed to accommodate, at the same time, the current transformers for the necessary protective relays and instruments.

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To obtain a maximum arc-interrupting effect, an optimum distance between the arcing contacts is required and it is, therefore, necessary to connect a movable isolating contact in series with the arcing contacts in order to provide a satisfactory isolating distance between the live breaker parts in the open breaker position. This separation of the arc-interrupting and isolating functions allows the arcing contacts to be designed relatively small and light, and this, in turn, makes it possible to obtain a very fast opening as well as a high-speed reclosing of the breaker by means of operating the arcing contacts only.

In cases where automatic high-speed reclosing operation is required, a separate insulator tube (13) is applied to supply compressed air for the automatic high-speed reclosing of the arcing contacts.

Breaker Operation

In the open position of the breaker, the isolating contact (9) is open while the arcing contacts (7) are held closed by spring pressure.

When the breaker is closing an auxiliary control valve becomes energized and feeds air from the breaker tank to the isolating contact piston (4), which closes the movable isolating contact (9) by means of a bevel gear drive and the rotating insulator (10).

When the breaker is tripped, compressed air is fed through an auxiliary control valve from the breaker tank to an auxiliary piston (3) in order to open the main blast valve (2), by means of which air flows with high velocity through the insulator column (5) into the arcing chamber (6) and opens the arcing contacts (7) pneumatically and extinguishes the arc at an early current zero. After the arc is extinguished, the air-saving piston (8) closes, and full air pressure is maintained between the open arcing contacts to provide a high dielectric medium which prevents reignition of the arc during the time when the movable isolating contact (9) opens. The pneumatic control is so arranged that, as soon as the isolating contact is fully open, the main air-blast valve closes again, and only the arcing contacts move back into closed position because of spring pressure.

In case automatic high-speed reclosure is applied, the arcing contacts open as on a normal tripping operation, but without operating the movable isolating contact (9), and they are held open under air pressure until, by means of an auxiliary air supply through the reclosing insulator tube (13), these contacts reclose again.

In case of a permanent fault, the breaker immediately opens again after the first automatic reclosure, in a similar way as on a normal three-phase tripping, by the opening of the arcing and isolating contacts, and will remain in open position.

150-Kv Air-Blast Circuit-Breaker Installation in Canadian Power Plant

Figure 2 shows the air-blast breaker installation of the Aluminum Company at Arvida. This breaker, designed for a service voltage of 150 kv and an interrupting capacity of 1,800,000 kva, has been in service since 1939. The large insulators in the foreground, upon which the stationary isolating contacts are mounted, also contain the current transformers which are necessary for the protective relays; otherwise the construction is the same as described previously. Automatic reclosing operation was not requested, and, therefore, this breaker was not equipped with reclosing device.

Arcing Chamber

The construction of the arcing chamber of this breaker is illustrated in Figure 3. Two arc breaks are connected in series which operate as follows:

When the main air-blast valve has opened, compressed air flows under high velocity through the hollow breaker insulator column and ducts into the outside space of the lower and upper arcing chambers, by which means the arcing contacts (1) move their full stroke away from the stationary arcing contacts (2), thereby striking the arcs. Compressed air rushes through the hollow movable arcing contacts and through the muffler (3) to the open air and envelopes the arcs by a stream of high-velocity air which extends the arcs while, by forceful cooling due to air expansion, the arc paths become deionized to such an extent that the arcs extinguish at an early current zero.

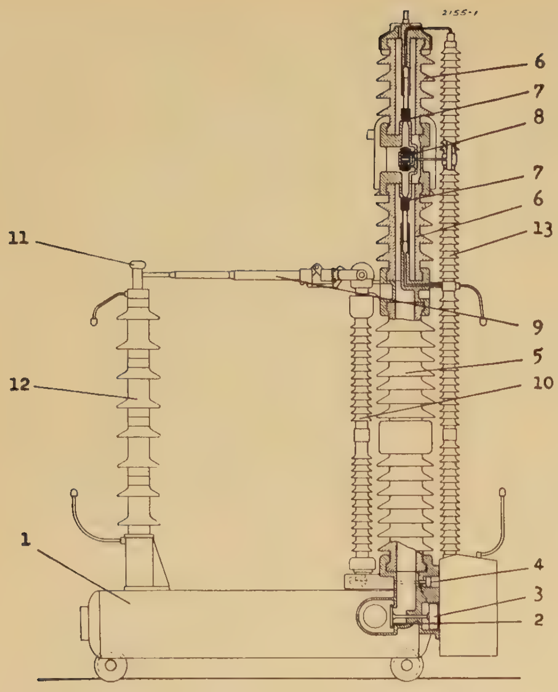


Figure 1 (left). Construction of 150-kv air-blast circuit breaker with double arc break

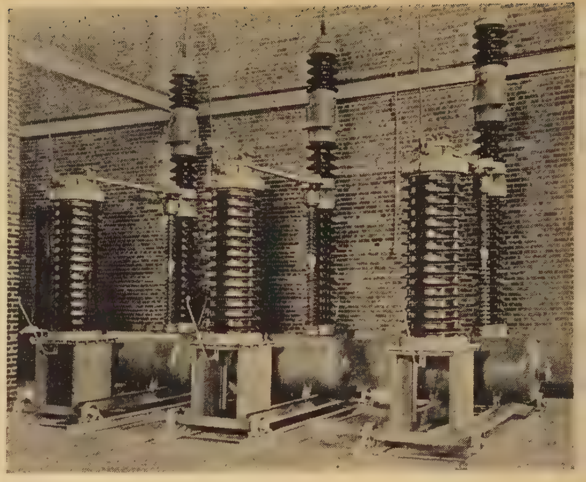


Figure 2 (right). 150-kv air-blast circuit-breaker installation of the Aluminum Company of Canada, Ltd.

When the arcing contacts move into the open position, some ports (4) become uncovered, through which compressed air can flow to the underside of the air-saving pistons (5), and, therefore, these pistons move against the arcing chamber partition, thus preventing a further escape of compressed air from the arcing chamber to atmosphere. A pneumatic time lag retards the closing of the air-saving pistons long enough to ensure positive extinction of the arcs. At the same time, when compressed air is fed into the arcing chamber, it is also admitted to an auxiliary piston which opens, with a time delay of a few cycles, the movable isolating contact. After the latter is fully open the main blast valve closes, and the air-saving piston returns to open position.

Operation

The operation of this type of air-blast circuit breaker is illustrated in oscillogram Figure 4, where a make-break operation is recorded. On a single-phase short-circuit at a voltage of 142 kv, a current equal to a three-phase interrupting capacity of 638 megavolt amperes was interrupted. As indicated on this oscillogram, the short-circuit was initiated at (1), at (2) the breaker tripping coil was energized, at (3) the arcing contacts were separated, and at (4) the arc interruption was completed. Including the arcing time of 0.007 second, the short-circuit interrupting time of the breaker proper (protection relay time excluded) amounted to 0.046 second.

The first high-voltage-type air-blast breaker in Canada was installed at the power distribution plant at Arvida in 1939 and has operated up to the present time very satisfactorily without any failure. A second 150-kv air-blast circuit breaker was installed at the same power distribution plant in 1941. This second breaker has also given excellent service. Experience has shown that no appreciable arcing-contact deterioration is caused even after several short-circuit interruptions. The inspection of the arcing contacts can be carried out in a few minutes, and the maintenance work on these breakers is very little compared to that on an oil circuit breaker.

150-Kv Air-Blast Circuit Breaker With Automatic Reclosing Device

Figure 5 illustrates the same type of air-blast breaker rated 150 kv but equipped with additional devices for automatic reclosing. Several breakers of this type are installed outdoors in European power distribution plants and have given very successful operating results on applying automatic high-speed single and three-phase reclosing.

Oscillogram Figure 6 illustrates a short-circuit interruption with high-speed reclosing which was carried out with this type of breaker on an overhead line of 150 kv. On the upper oscillogram the interruption of an arcing short circuit is shown with automatic reclosing. Experience has shown that restriking of transient system faults could be prevented in most cases if the breaker stayed open approximately 10 to 15 cycles. Therefore, the breaker reclosing time was set in such a way that the breaker was delayed in reclosing approximately 0.19 second. With this time delay in regard to the automatic reclosing, very satisfactory operating results were obtained. At a voltage of 150 kv, a current of 2,200 amperes was interrupted. The total short-circuit interrupting time of the breaker proper (protective relay time excluded) was 0.043 second.

The lower oscillogram of Figure 6 shows the interruption of a metallic permanent short circuit. After the first arc interruption is finished at (2), the breaker recloses automatically only once at (3), and then immediately opens a second time by a final trip and stays open.

Breaker Operation at Low Temperature and Under Heavy Ice Formation

In order to check that the movable breaker parts and piston mechanism operate reliably even under heavy snow and

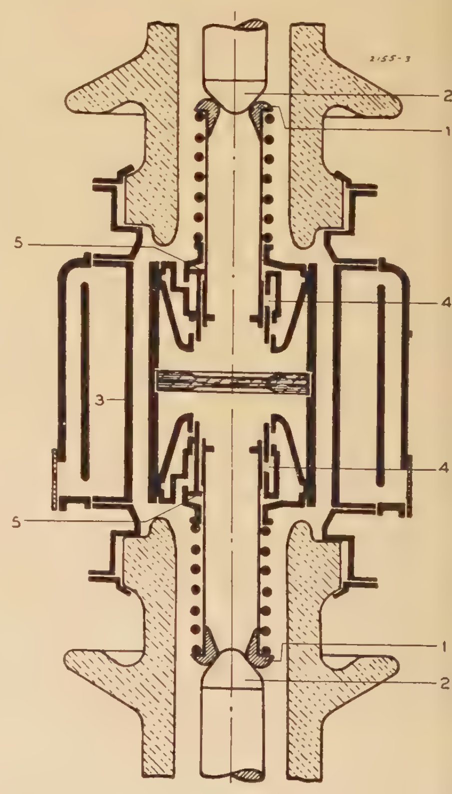


Figure 3. Arcing chamber of 150-kv air-blast circuit breaker

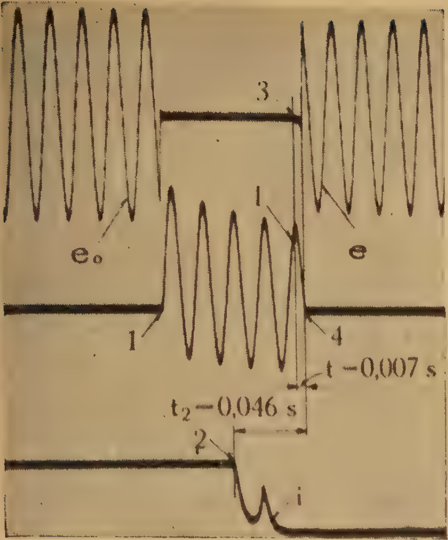
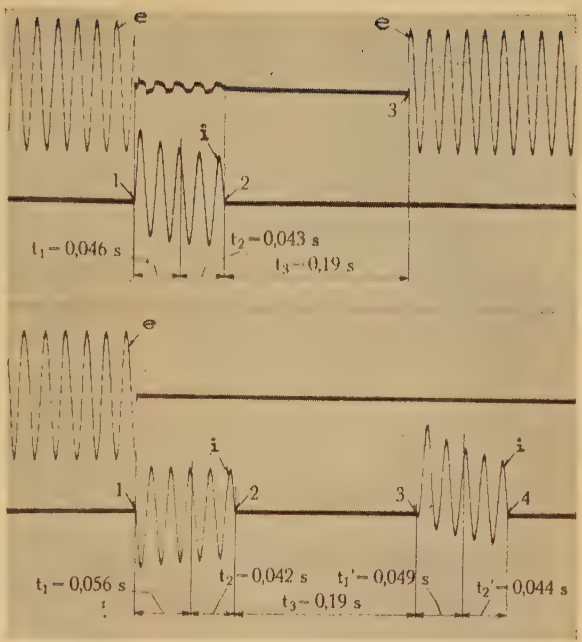


Figure 4. Make-break operation on a single-phase short-circuit by 150-kv air-blast circuit breaker

e_0—Line voltage (142 kv)
e —Recovery voltage (130 kv)
I —Fault current (2,450 rms amperes)
i —Tripping-coil current

Figure 6. Short-circuit interruption with automatic reclosing by 150-kv air-blast circuit breaker

e—Line voltage (150 kv)
i—Fault current (2,200 rms amperes)



ice formation, extensive tests under similar conditions as shown in Figure 7 have been carried out with various high-voltage air-blast breakers, and it was found that, even under extremely heavy snow and ice formation, all the movable breaker parts operated very satisfactorily, both as to tripping and closing of the breaker.

In order to prevent condensation and ice formation inside the outdoor air-pipe system and breaker parts, special air-drying equipment was applied through which the compressed air is filtered and dried. Furthermore, an additional outdoor-erected air-storage tank was used in order to refill the breaker air tank; at each breaker operation, with air at approximately the same temperature as the breaker parts.

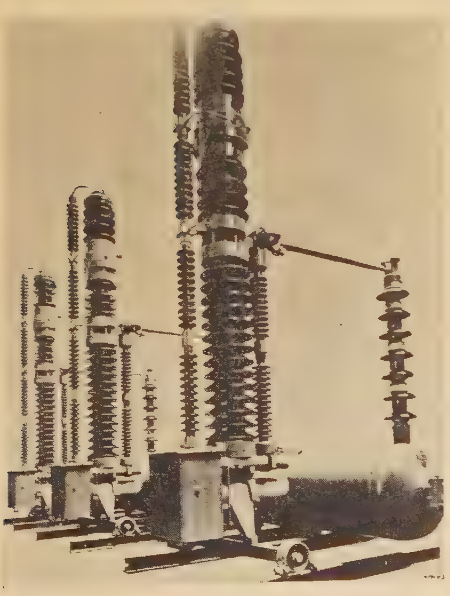


Figure 5. 150-kv outdoor air-blast circuit breaker with automatic high-speed reclosing devices

220-Kv Air-Blast Circuit Breaker Installed Outdoors on a Canadian High-Voltage Power Line

Figure 8 shows a 220-kv air-blast high-speed circuit breaker installed in the outdoor switching station of the La Tuque power plant of the Shawinigan Water and Power Company. This breaker is rated for an arc-interrupting capacity of 2,500,000 kva at 220 kv and is equipped with additional devices for carrying out single-phase or three-phase automatic high-speed reclosing.

Construction

Each breaker phase has a separate undercarriage upon which two large insulator columns are mounted; to each breaker undercarriage are attached four air receivers and a complete control valve and piston assembly as required for the operation of each individual breaker pole. Each insulator column consists of a large foot insulator upon which a double arcing-contact chamber is mounted, thus forming four arc breaks in series for each breaker phase. Inside the foot insulator are located two porcelain pressure tubes for supplying compressed air for the arc interruption and for the high-speed reclosing. Furthermore, to each insulator column is attached a movable isolating arm which is operated by means of rotating insulator and bevel gear drive from a separate piston on closing and normal tripping. Otherwise, the design of each insulator column is similar to that described on the 150-kv air-blast circuit breaker.

Arcing Chamber

The arcing chamber, mounted on top of the foot insulator, is illustrated in Figure 9. Two movable arcing contacts (1) and two stationary contacts (2) are mounted inside two porcelain insulators (3), which are surrounded by capacitance tubes (6) which control the potential

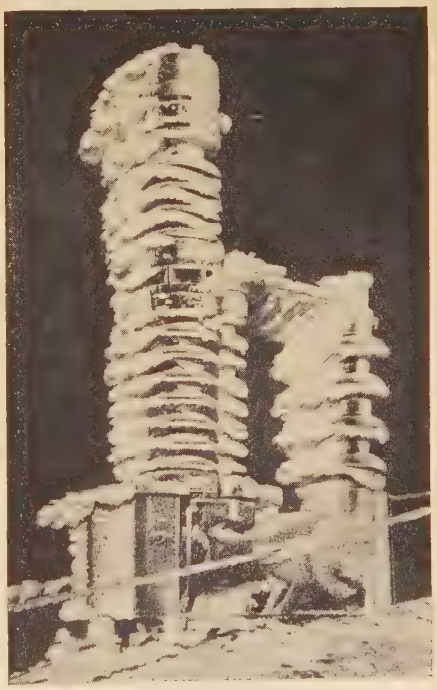


Figure 7. Air-blast circuit-breaker tests under heavy snow and ice formation

Figure 8. 220-kv air-blast circuit-breaker installation of The Shawinigan Water and Power Company

distribution between the individual arc breaks. The movable arcing contacts (1) are operated by means of a differential piston (7) and a lever arrangement (5); the latter assures simultaneous opening of the arcing contacts in series. In the intermediate piece (4) and also in the cast-iron piece (8) located at the top and at the bottom of the fixed arcing contacts, are the air-saving pistons (9), through which the compressed air in the arcing chamber escapes to atmosphere.

Operation

When the breaker is tripped by the opening of the main blast valve, compressed air is supplied through the large porcelain pressure tube (10) and ducts into the outside space of the lower and the upper arcing chambers and also through a duct into the intermediate piece (4) behind the differential piston (7). As soon as the air pressure is built up, this differential piston opens the arcing contacts by compressing the contact spring (11), and a stream of air flows with high velocity through the hollow arcing contacts and the air-saving pistons to atmosphere, which creates a rapid deionization of the arcing path and extinguishes the arc at next zero passing. At the same time part of the compressed air also flows through a special duct into the air-filling volume (12), which closes the air-saving pistons with a certain time delay, thus preventing a further escape of compressed air to the outside after the arc is extinguished. Closing the air-saving pistons, the compressed air re-establishes a high dielectric strength between the arcing contacts and, therefore, prevents reignition by the recovery voltage. At the same time, when compressed air is supplied to the arcing chambers, air is admitted also to the isolating contact piston which opens, with a short time delay, the movable isolating contact under no-load condition. After the complete opening of the isolating contacts, the main blast valve closes again, the arcing contacts close, and the air-saving pistons open again by spring pressure.

On tripping with automatic high-speed reclosing, only the arcing contacts open as previously described—but without opening the movable isolating contact. The arcing contacts are held open under air pressure until, by means of an auxiliary reclosing piston, air is admitted through the porcelain pressure tubes (13, 14) to the differential piston (7) and air-saving pistons (9), which simultaneously reclose the arcing contacts and open the air-saving pistons.

The following operations are carried out by the breaker protective and control devices:

By means of control relays, the breaker opens at the first tripping impulse, either single or three-phase, by the arcing contacts only, and recloses automatically upon an auxiliary reclosing relay impulse with a definite time-delay which can be adjusted to any value most suitable for the particular network. In case of a single-phase tripping by a permanent single-phase short-circuit, the breaker automatically recloses only once on the faulty phase, and immediately opens again by a final three-phase trip with the arcing and isolating contacts and stays open.

By means of a control and minimum air-pressure valve, it is assured that the breaker operates only if the available pressure of the compressed air, stored in the breaker tanks, is in the range of the admissible operation pressure. If on any reclosing the air pressure drops lower than normal, the reclosing function will not be carried through, and the breaker opens immediately by a three-phase final tripping, which will be initiated by means of a contact pressure gauge.

If while closing the breaker, an urgent trip is required, the air to the closing piston will be shut off immediately by means of an anti-pumping valve, which allows the tripping operation to be carried out without delay. By these various protective and control devices, it is assured that the breaker will always carry out the best suitable breaker function in connection with the particular operating condition.

Test Results

Field tests have been carried out with this installation where the principle functions were recorded simultaneously on all three breaker poles. Furthermore, by high-voltage short-circuit tests, the arc interrupting ability of the breaker was checked.

The upper oscillogram of Figure 10 illustrates the operation of this breaker in case of a permanent single-phase fault. Only the faulted phase opens initially, followed by automatic reclosure after a short time delay. Thereupon, the breaker immediately opens again, this time on all three phases, both arcing and isolating contacts, and remains opened. As shown on the lower oscillogram of Figure 10, a similar sequence is carried out for a permanent three-phase fault, except that in this case all three phases are opened on the first as well as on the second opening. The recorded potential of the travel indicator, which was attached to the rotating insulator of the movable isolating contact, shows that at the first interruption the movable isolating contact did not move, while at the second, final interruption this contact opened a few cycles after the arcing contacts parted.

From these oscillograms, Figure 10, the following breaker speeds were obtained: The breaker opening time until the arcing contacts part is $2^3/_4$ to 3 cycles. The automatic reclosing is delayed 23 cycles on single-phase and 16 cycles on three-phase circuit interruption, but this delay can be set to any desired value by means of a relay with an adjustable time-delay device. The movable isolating contact parts approximately four cycles after the opening of the arcing contacts.

For testing the arc-interrupting ability of this breaker, special short-circuit tests were carried out after the installation of this breaker was completed. At first, sev-

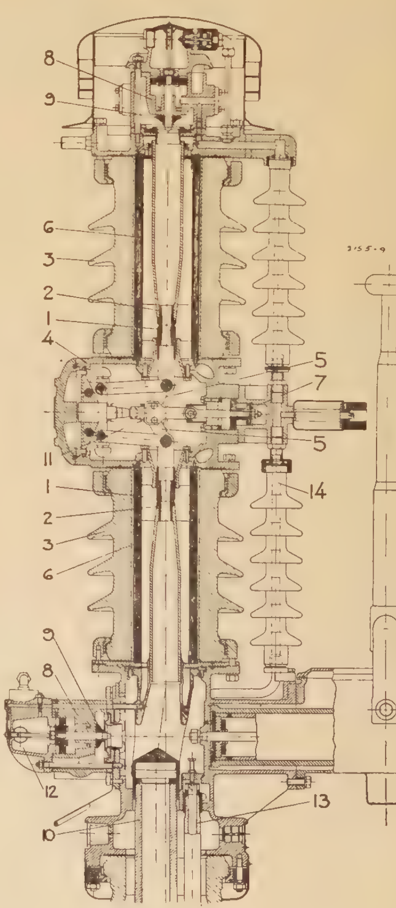


Figure 9. Arcing chamber of 220-kv air-blast circuit breaker

eral short-circuit tests (single-phase-to-ground) were carried out during which the line voltage was gradually raised from 220 kv to 260 kv. A fault current of approximately 280 to 330 amperes was interrupted without any difficulty, with an arcing time of less than one cycle.

In addition, similar short-circuit tests were carried out on the network with the power plants connected in parallel so as to obtain the maximum possible short-circuit capacity. With all these networks in parallel, the breaker interrupted a short circuit which was equal to a three-phase interrupting capacity of approximately 1,000,000 kva as illustrated in oscillogram Figure 11. The time required for the protective relay and the auxiliary contacts of the control circuit of the particular test arrangement was $3^1/_2$ cycles, the breaker opening time proper, including arcing time, was only $4^1/_2$ cycles, and the arcing time was less than one cycle.

Tests also were carried out with all networks in parallel where the breaker was switched twice into a short circuit by automatic high-speed reclosing with a time delay of only 17 cycles between the first and the second arc interruption, and twice a short-circuit current of 2,740/2,330 amperes at approximately 220 kv was interrupted satisfactorily with an arcing time at both interruptions of approximately one-half cycle. At the first interruption (which was equal to a three-phase interrupting capacity of approximately 1,000,000 kva) the arcing contacts opened without opening of the isolating contacts and, therefore, the full recovery voltage of approximately 220 kv had to be held between the small distance of the four arc breaks in series until the breaker automatically reclosed again.

All these tests, including the test with automatic reclosing, had so little influence on the large network system that these short circuits could hardly be noticed at the different power stations. Full stability of the networks was maintained through all these various short-circuit tests.

After several weeks of satisfactory operation with this breaker, a flashover took place inside the small porcelain pressure tube for automatic reclosing, and this put the breaker out of operation until the replacement of the damaged parts. It is assumed that probably condensation on the surface of this porcelain tube led to this flashover.

From the various tests and operating results we may conclude that these high-voltage air-blast breakers of 150 and 220 kv have shown the advantage of interrupting large short-circuit capacities with an air stream of high velocity and the facility of carrying out automatic high-speed single and three-phase reclosing.

Latest Design of an Air-Blast Circuit Breaker for Extremely High Voltage and High Interrupting Capacity

Based on experience from the high-voltage air-blast circuit breakers installed in various power plants and on extensive high-voltage arc-interrupting tests carried out in their large breaker test room,

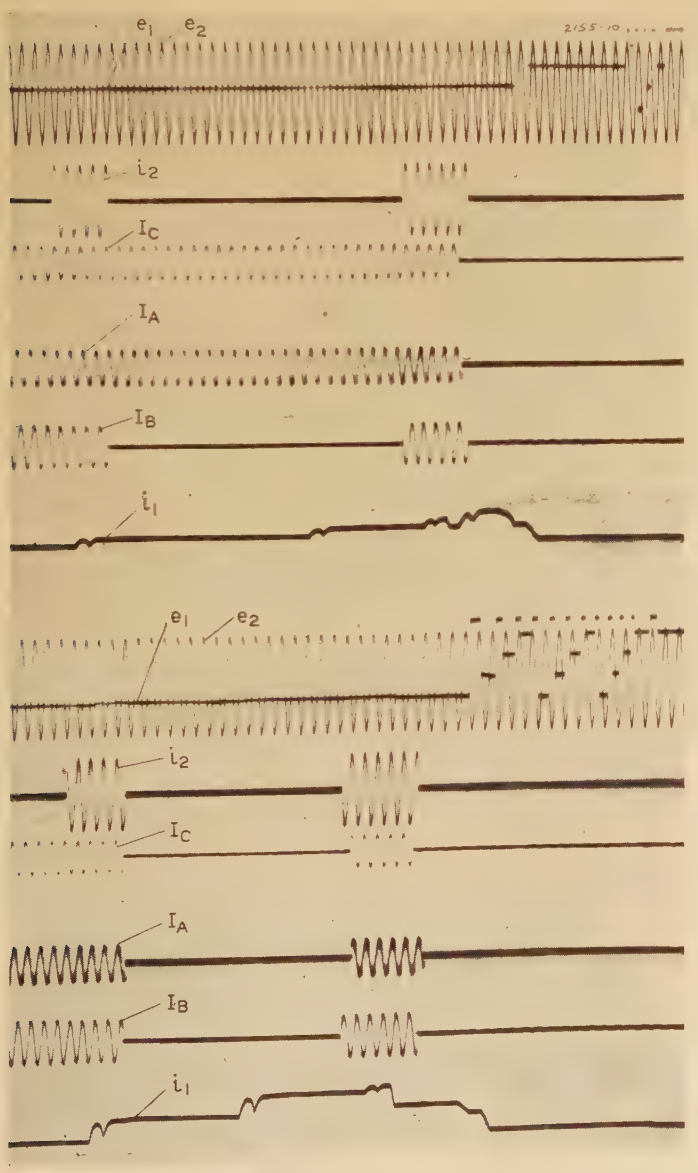
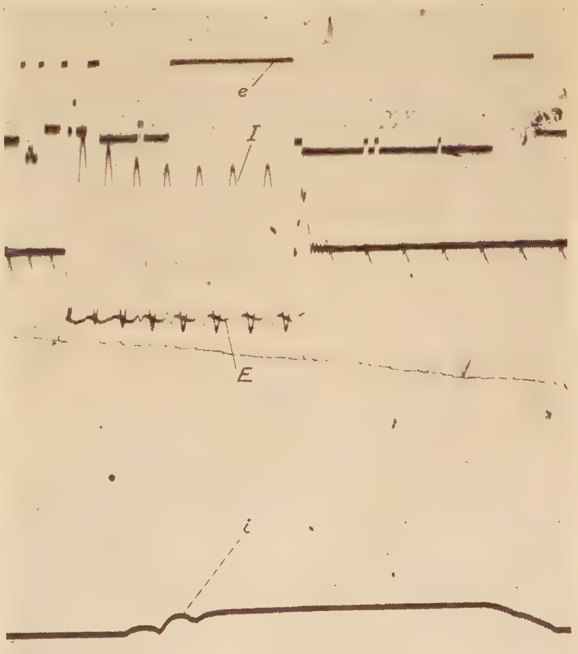


Figure 10 (left). Automatic single-phase and three-phase reclosing with final three-phase tripping by 220-kv air-blast breaker

i_1—Breaker control impulses
I_B, I_A, I_C—Current of breaker phases
i_2—Tripping relay impulse
e_1—Travel indicator potential
e_2—Timing wave (60 cycles)

Figure 11 (below). Interruption of a single-phase short-circuit to ground at 220 kv by air-blast breaker (interruption equal to three-phase interrupting capacity of 1,000,000 kva)

i—Breaker control impulses
E—Compensated voltage across fault
I—Fault current phase B
e—Travel indicator potential



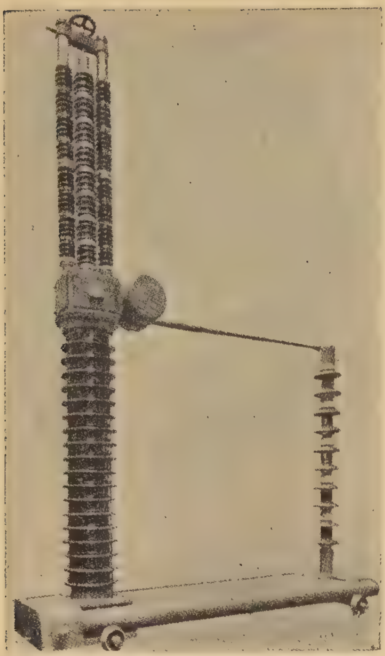


Figure 12. Air-blast circuit breaker for extremely high voltage and high interrupting capacity with eight arc breaks in series

Brown, Boveri and Company have developed, in addition to the previously described air-blast breakers of 150 and 220 kv, an air-blast breaker for extremely high voltage and high interrupting capacity as illustrated in Figure 12.

Construction

On this breaker eight potential-controlled arc breaks are connected in series, which open simultaneously by means of compressed air, and which provide an extremely short arc interruption. In regard to the various pistons and other breaker parts, the design in principle is

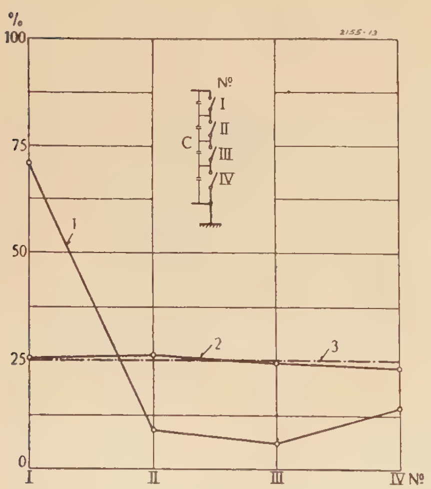


Figure 13. Potential distribution across four arc breaks in series

1. Without potential control
2. With potential control
3. Ideal distribution of potential

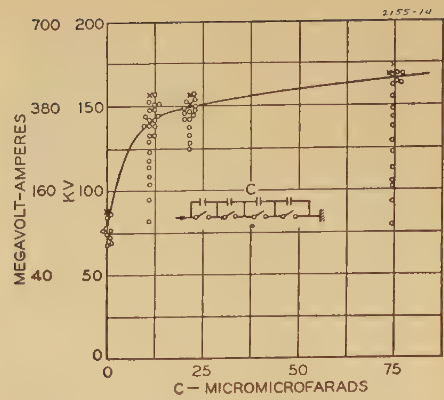


Figure 14. Increase in interrupting capacity and interrupting voltage in function of the control capacitance (C) across the four arc breaks in series

similar to the previously mentioned design but simplified to a large extent. The inspection of the individual arcing contacts can be carried out in a few minutes, since it only requires loosening the spindle of a press clamp for taking off the contacts for inspection.

This breaker is also equipped with high-speed reclosing devices, by means of which single and three-phase high-speed reclosing can be carried out as previously described.

Breaker Tests

The design of this breaker has resulted from the following tests, which give proof of the interrupting ability of this breaker. Extensive investigations have shown that the application of arcing chambers with potential-controlled multiple arc breaks presents the best solution for an air-blast breaker of extremely high voltage and high interrupting capacity, because it allows re-establishment of the dielectric strength across the arcing path at several breaks simultaneously, which extinguishes the arc very effectively. With the rise of the recovery voltage after arc interruption, the distribution of the voltage across the arc breaks is influenced by the charging currents of the capacitances across the breaks and to earth and, therefore, it is of paramount importance to assure an equal distribution of the voltage across the different arc breaks by means of connecting small capacities across the individual breaks.

The test results, as illustrated in Figure 13, show clearly that the highest voltage across one break (with four uncontrolled arc breaks in series) can rise as high as 71 per cent of the total voltage (as shown by curve 1) while, if all four arc breaks are potential-controlled by means of small capacitances, very equal potential distribution between the four arc breaks is attained (as shown by curve 2).

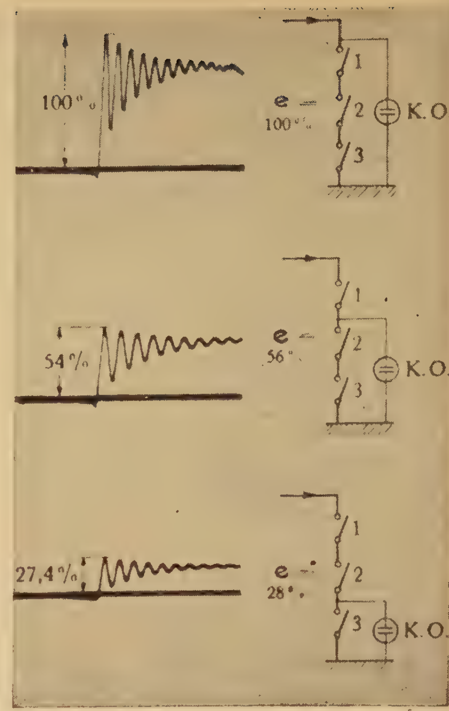


Figure 15. Oscillations of the recovery voltage across potential-controlled arc breaks during the breaker opening, recorded by cathode-ray oscillograph

e—Distribution of the potential during the oscillation

KO—Cathode-ray oscillograph

The test data, illustrated in Figure 14, show that the admissible interrupting capacity can be considerably increased with increasing values of the small capacitances which are connected between the individual arc breaks.

Furthermore, it has been proved by potential measurements carried out with a cathode-ray oscillograph (as illustrated in Figure 15) that during the opening of potential-controlled arcing contacts, the equal distribution of the potential is fully maintained and, therefore, the stresses on

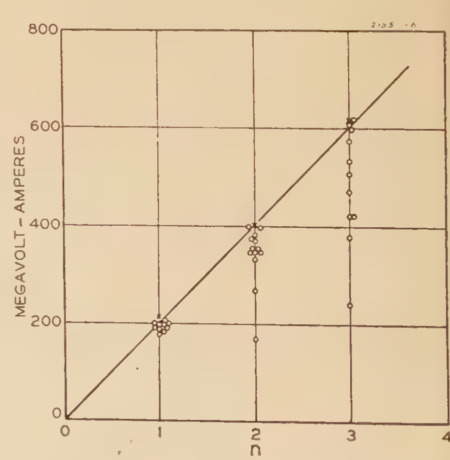


Figure 16. Increase in interrupting capacity in function of the number of potential-controlled arc breaks

Method for A-C Network Analysis Using Resistance Networks

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THE mathematical solution of a-c network problems, of the type known as load distribution and voltage studies, is difficult and tedious for complex networks.

Where available, the a-c network analyzer or calculator affords the means of solving this type of problem, as well as many other problems, with good speed and accuracy.

It is the purpose of this paper to describe a new method of calculating a-c network problems and to illustrate with a practical example how a conventional d-c board, comprised of resistance circuit elements, may be used to solve load distribution and voltage studies. The writer has developed a new type of d-c board which would facilitate the use of the method described in this paper, but for the purpose of the present paper the application is limited to the existing types of d-c boards.

The scope of the present paper is restricted to the analysis of three-phase networks with balanced loading on the three phases. If the application to load distribution studies is understood, it is believed the use of the method for short-circuit studies will be evident. For short-circuit work this method can be used to advantage if the resistance of the network circuits is appreciable, so that a network setup representing reactance does not give sufficiently accurate results, and where a setup representing impedance is inaccurate because of differences in ratio of X to R for the circuits forming the network.

In contrast with the a-c network analyzer, inherently this method is not exact, but by successive approximations an exact solution may be obtained. Fortunately the first approximation is close enough for most practical problems, and rarely is it necessary to go further than the second approximation to obtain the desired accuracy. As the illustration shows, the method is simple and is quicker than other methods in analyzing complex networks with the exception of the a-c analyzer. Therefore the writer believes that the method of this paper as applied to a d-c board can be a useful tool for engineers in system planning work.

Simple computations are involved to obtain the voltage drop in a radial circuit with known line constants and loading, particularly when the circuit may be treated as a lumped impedance.

When a current with inphase component $(+I_p)$ and quadrature component $(+I_q)$ flows through a circuit of resistance R and reactance X, the inphase component of voltage drop is $V_p=I_pR-I_qX$, and the quadrature component is $V_q=I_qR+I_pX$. The components of current and voltage drop refer to the same reference axis.

If a current equal to I_p is passed through a resistance equal to R, the voltage drop I_pR can be measured. If a current equal to I_q is passed through a resistance equal to X, the voltage drop I_qX can be measured. Likewise passing I_p through X and I_q through R, I_pX and I_qR can be measured, and, by using the equation for V_p and V_q, the values of the components of voltage drop are obtained. These simple equations are the basis for the method of this paper.

When these equations are applied to network analysis, it is necessary to choose a reference voltage at one point in the network to which all voltage and current components refer for phase position. For simplicity, a network with balanced loading, is analyzed on a single-phase-to-neutral basis. To obtain a correct solution, it is necessary to satisfy Kirchhoff's laws for a-c network, which may be expressed as follows:

(*a*). The algebraic sum of the components of current toward any junction point is zero.

(*b*). The algebraic sum of the components of voltage around any closed path in the network is zero.

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each individual arc break are in proportional relation to the total interrupting capacity.

As a result of these tests, a definite conclusion can be drawn regarding the performance of the breaker under its rated interrupting capacity by applying the proportional interrupting capacity on one break only. This contention has been proved by the test data shown in Figure 16 where it is shown that the maximum admissible interrupting capacity increases practically in proportion to the number of potential-controlled arc breaks. For high-voltage circuit breakers, relatively high interrupting capacities are required which far exceed the maximum short-circuit capacity available at the present test plants. It is, therefore, of great importance to note from these tests that a breaker with potential-controlled multiple arc breaks can be tested accurately by interrupting the partial load with one break only, which enables one to judge what the performance of this breaker would be under full rated interrupting capacity.

Therefore, judging from these test results, it should be possible today to design, for example, an air-blast circuit breaker for a voltage as high as 500 kv without requiring a test plant of such a high voltage.

Conclusion

The construction of the various high-voltage air-blast breakers and the operating and test data show that today this type of breaker is developed to such an extent that very valuable operating results can be obtained. In summarizing, we can conclude that, besides lowering the maintenance cost, the stability of the transmission systems can further be increased because of the breaker interrupting speed and the possibility of carrying out high-speed reclosing.

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In the practical network problem which follows, the compliance with Kirchhoff's laws is illustrated; however, it should be noted that in this particular problem, the transformers involved have the same ratio, so that there is no boost or buck in the voltage in any closed path in the network. The method of treating boost or buck in a closed path will be discussed later in the paper.

Application of Method to Network Problem

Following is an outline of procedure for making a load distribution and voltage study for a complex network with the aid of a conventional d-c calculating board and illustrated with a practical problem. The network shown on Figure 1 consists of 116 miles of 115-kv and 395 miles of 57-kv three-phase transmission circuits. The actual systems involved have more circuit mileage which is radial with respect to the network shown and so is not included in the problem. The estimated loads and generation for the December 1942 peak are used in the study with a total megawatt input in excess of 200 megawatts; however due to simplification by subtracting local load from generation, the net total input is reduced to 157 megawatts for the purpose of the network problem.

1. One-Line Diagram of Network

Figure 1 is the one-line diagram of the network simplified somewhat from the actual system to eliminate radial lines which are set up as loads at the network busses.

The method of lettering busses and numbering circuits as shown on Figure 1 is a satisfactory means of simplifying the recording of data and lessening the chance of making errors in the direction of current flow or voltage drop in the network circuits.

2. Circuit Constants

The circuit constants R, X, B, and G are required as shown in Table I. The constants are on a common phase voltage base of 57 kv in the problem but may be on any suitable common voltage or kilovolt-ampere base, if the currents are on the same base. (Where parallel circuits interconnect busses, the constants of the equivalent circuit will simplify the board setup, and if the parallel circuits have ratios of X/R which are substantially different, it is essential that the equivalent circuit be used, otherwise additional successive approximations may be necessary to obtain the desired accuracy in the study.)

3. Loads and Estimated Generation

As shown in Table II and on Figure 1, the real and reactive power should be tabulated at all load busses. Wherever generator or synchronous condenser inputs to the network occur at load busses, the input and load real and reactive power should be combined to give the net load or generation at that point. It should be noted that in Table II the leading reactive power at load busses is moved into the generation column, so that all reactive will be at the same algebraic sign, negative.

In the problem the voltage at the electrical load center of the system is 57.7 kv, so that the reference voltage to neutral is chosen as 33.33+j0 kv. Bus P on Figure 1 is the main receiving station for the generating plants and would be expected to have a voltage phase angle approximately that of the major loads, so that it is chosen as the reference bus. Therefore all voltage and current components for the network will refer to this same reference voltage, E_n'=33.33+j0 kv. Since the components of voltage to neutrals E_p and E_q are unknown at the other load busses of the network, E_n' is assumed to exist at all load busses, so that the current components I_p and I_q can be computed as shown in Table II. At generator busses remote from the load center, the values of I_p and I_q are computed on the basis of estimated inputs to the network and estimated voltages, but, as shown in Table II, these input currents in total must equal the sum of the load currents.

4. Setup on D-C Board to Determine Voltage Drops

The resistance units of the d-c board are now set up to simulate the resistance or R network on the basis of the R values of Table I. The positive bus of the board is connected through a rheostat to a common bus from which all I_p input busses are fed through individual rheostats. The negative bus of the board is connected to a common bus to which all output I_p currents flow from their respective load busses of the network through rheostats. The board is then ready for adjustment to set up I_p input and output currents in some convenient proportion to the actual I_p currents shown in Table II. It is important to es-

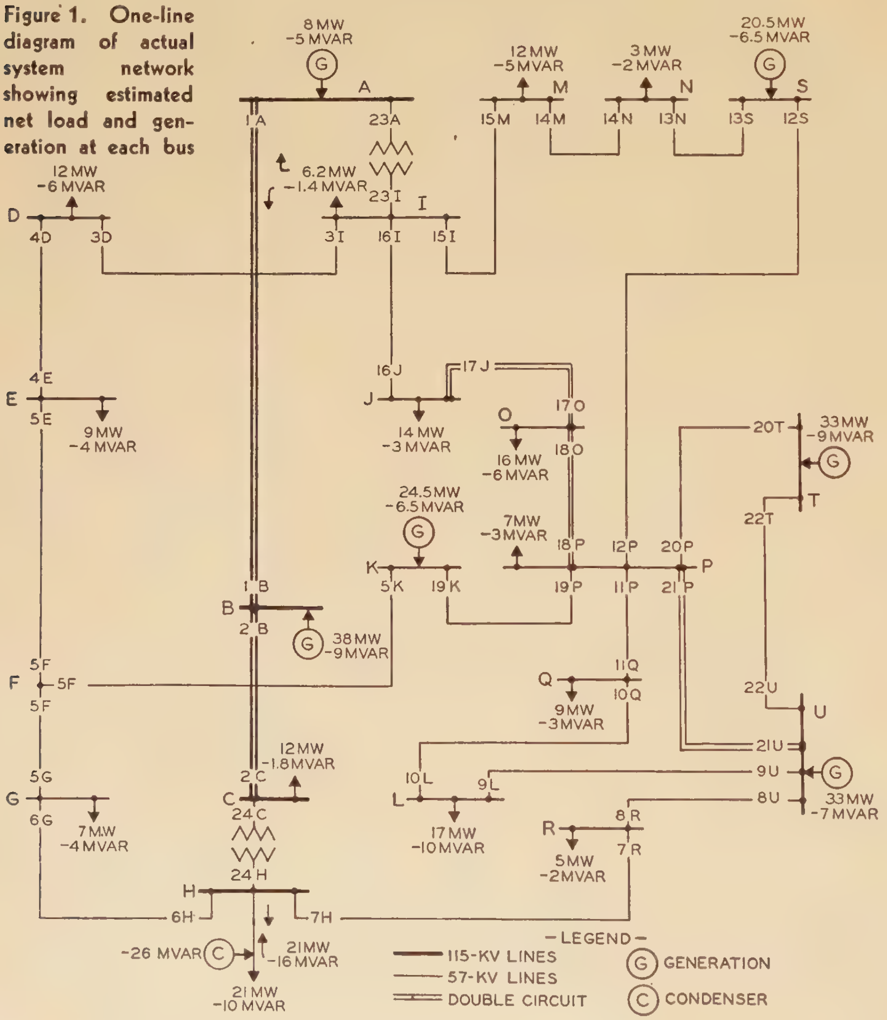

Figure 1. One-line diagram of actual system network showing estimated net load and generation at each bus

Table I. Network Problem—Circuit Constants on 57-Kv Base

Circuit Designation	Resistance, R Ohms to Neutral	Reactance, X Ohms to Neutral	Conductance G = R ÷ (R²+X²)	Susceptance B = X ÷ (R²+X²)
1*AB*	0.5	2.0	0.1175	0.4710
2*BC*	1.0	3.5	0.0755	0.2640
3*ID*	2.5	4.0	0.1125	0.1798
4*DE*	12.0	12.5	0.0400	0.0417
5*EF*	3.5	4.0	0.1240	0.1420
5*FK*	3.5	4.5	0.1077	0.1384
5*FG*	15.0	23.0	0.0199	0.0305
6*GH*	9.0	14.5	0.0309	0.0498
7*HR*	12.0	15.5	0.0313	0.0405
8*RU*	17.0	24.0	0 0197	0.0278
9*UL*	9.0	13.5	0.0342	0.0513
10*LQ*	5.0	7.0	0.0676	0.0946
11*QP*	1.5	4.0	0.0822	0.2190
12*PS*	11.0	14.5	0.0332	0.0438
13*SN*	8.5	10.5	0.0467	0.0577
14*NM*	8.0	9.5	0 0520	0.0617
15*MI*	3.5	5.0	0.0940	0.1340
16*IJ*	4.5	7.0	0.0650	0.1010
17*JO*	1.5	2.0	0.2400	0.3200
18*OP*	1.0	1.5	0.3080	0.4620
19*PK*	2.5	7.0	0 0453	0.1267
20*PT*	11.5	34.0	0.0089	0.0264
21*PU*	4.5	9.0	0.0450	0 0890
22*TU*	5.0	15.0	0.0200	0.0600
23*AI*	0.5	4.5	0.0244	0.2192
24*CH*	1.0	8.0	0 0154	0.1231

Table II. Network Problem—Loads and Generation for Setup on D-C Calculating Board

Bus	Loads		Generation (Estimated)		Load Current		Generated Current	
	Megawatts	Megavars	Megawatts	Megavars	I_p	I_q	I_p	I_q
A	—	—	8.0	− 5.0	—	—	80	$-j41$
B	—	—	38.0	− 9.0	—	—	380	$-j74$
C	12.0	− 1.8	—	—	120	$-j18$	—	—
D	12.0	− 6.0	—	—	120	$-j60$	—	—
E	9.0	− 4.0	—	—	90	$-j40$	—	—
F	—	—	—	—	—	—	—	—
G	7.0	− 4.0	—	—	70	$-j40$	—	—
H	21.0	*	—	−16.0	210	*	—	$-j135$
I	6.2	*	—	− 1.4	62	*	—	$-j12$
J	14.0	− 3.0	—	—	140	$-j30$	—	—
K	—	—	24.5	− 6.5	—	—	245	$-j54$
L	17.0	−10.0	—	—	170	$-j100$	—	—
M	12.0	− 5.0	—	—	120	$-j50$	—	—
N	3.0	− 2.0	—	—	30	$-j20$	—	—
O	16.0	− 6.0	—	—	160	$-j60$	—	—
P	7.0	− 3.0	—	—	70	$-j30$	—	—
Q	9.0	− 3.0	—	—	90	$-j30$	—	—
R	5.0	− 2.0	—	—	50	$-j20$	—	—
S	—	—	20.5	− 6.5	—	—	193	$-j52$
T	—	—	33.0	− 9.0	—	—	300	$-j73$
U	—	—	33.0	− 7.0	—	—	304	$-j57$
Totals	150.2	−49.8	157.0	−60.4	1,502	$-j498$	1,502	$-j498$
Estimated loss =	6.8	−10.6						
Estimated input =	157.0	−60.4						

*Leading or + megavars at loads is shown as − megavars generation.

At loads $I_p = 1{,}000 \times \text{mw}/57.7\sqrt{3} = 10 \times \text{mw}$.

At loads $I_q = 1{,}000 \times \text{mvar}/57.7\sqrt{3} = 10 \times \text{mvar}$.

For input or generation to network, estimated bus voltages are used to compute I_p and I_q from the estimated megawatts and megavars input, but it is essential for the d-c board setup that the totals of I_p and I_q input are equal to the totals supplied to loads.

tablish a small current through the board at the start by adjusting the rheostat between the positive bus and the common input bus to have a high value of resistance as compared to the network resistances, so that it will act as a current limiter or, in effect, help maintain an almost constant total current to the network. Actually it would be better if a constant current supply could be used, but the current limiting rheostat referred to is satisfactory for the purpose. After the I_p currents into the network at generator busses and out of the network at load busses have been adjusted, by means of their respective generator and load rheostats, to be proportional to the desired values of Table II, the resistance of the current-limiting rheostat may be reduced to the point where a substantial current flows through the network. With more current it is then possible to adjust generator and load rheostats slightly to give the values of currents, still proportional to those of Table II, which are to be the basis for the readings of $I_{pr}R$ voltage differences between bus *P* and all other busses of the network. For example ($I_{pr}R$) measured from bus *A* to bus *P* multiplied by the constant for the d-c board for this set of readings is equal to −635 volts and is recorded in Table III. The board constant depends on the proportionality factors between board resistances and actual network resistances, between board currents and actual network currents, and also on the type of scale calibration on the voltmeter, so that the data for this study are simplified by the elimination of the board constants from the tabulations.

In a similar manner the circuit resistance units are adjusted to simulate the reactance or *X* network on the basis of the *X* values of Table I. Again by use of the current-limiting rheostat the board current is reduced, and some minor adjustments in generator I_p and load I_p currents will make them proportional to those of Table II, and the same procedure as described for obtaining $I_{pr}R$ voltage readings is followed to obtain $I_{px}X$ voltage differences between all busses and reference bus *P*. These readings are in Table III.

In like manner ($-I_{qx}X$) readings can be obtained. However it will be noted in this problem, and in most problems of

Table III. Network Problem—Measured Components of Voltage Drop Between All Busses and Reference Bus *P*

*	($+I_{pr}R$)	($-I_{qx}X$)	$V_p = I_{pr}R - I_{qx}X$	($+I_{px}X$)	($-I_{qr}R$)	$V_q = I_{px}X + I_{qr}R$
AP	−635	+865	+230	+125	+335	−210
BP	−595	+1,055	+460	+340	+380	−40
CP	−895	+1,135	+240	−605	+435	−1,040
DP	−960	+40	−920	−1,130	+80	−1,210
EP	−760	+70	−690	−820	−70	−750
FP	−405	+235	−170	−315	+25	−340
GP	−1,200	+620	−580	−1,840	+110	−1,950
HP	−1,075	+1,435	+360	−1,825	+505	−2,330
IP	−710	+260	−450	−735	+255	−990
JP	−550	−60	−610	−730	−10	−720
KP	+195	+345	+540	+445	+85	+360
LP	−275	−455	−730	−395	−305	−90
MP	−820	+40	−780	−900	+90	−990
NP	−120	+90	−30	−100	+120	−220
OP	−280	−90	−370	−440	−50	−390
QP	−170	−240	−410	−375	−105	−270
RP	−575	+865	+290	−880	+230	−1,110
SP	+890	+360	+1,250	+1,120	+320	+800
TP	+1,730	+1,050	+2,780	+4,410	+380	+4,030
UP	+1,020	+450	+1,470	+1,785	+175	+1,610

* ($+I_{pr}R$) is measured with $+I_p$ currents flowing through *R* network.
($-I_{qx}X$) is measured with $+I_q$ current flowing through *X* network.
($+I_{px}X$) is measured with $+I_p$ current flowing through *X* network.
($-I_{qr}R$) is measured with $+I_q$ current flowing through *R* network.

Table IV. Network Problem—Computation of Components of Current Flowing in Each Circuit

Circuit Designation	Voltage Difference V_p	Voltage Difference V_q	$I_p=GV_p+BV_q$: GV_p	BV_q	I_p	$I_q=GV_q-BV_p$: GV_q	BV_p	I_q
1*BA*	+230	+170	+27.0	+80.0	+107.0	+20.0	+108.0	−88.0
2*BC*	+220	+1,000	+16.8	+264.0	+280.8	+75.5	+58.1	+17.4
3*ID*	+470	+220	+52.9	+39.6	+92.5	+24.7	+84.4	−59.7
4*ED*	+230	+460	+9.2	+19.2	+28.4	+18.4	+9.6	+8.8
5*FE*	+520	+410	+64.5	+58.2	+122.7	+50.8	+73.9	−23.1
5*KF*	+710	+700	+76.4	+97.0	+173.4	+75.4	+98.5	−23.1
5*FG*	+440	+1,610	+8.8	+49.1	+57.9	+32.0	+13.4	+18.6
6*HG*	+970	−380	+30.0	−18.9	+11.1	−11.1	+48.4	−60.2
7*HR*	+70	−1,220	+2.2	−49.5	−47.3	−38.3	+2.8	−41.1
8*UR*	+1,180	+2,720	+23.2	+75.8	+99.0	+53.6	+32.9	+20.7
9*UL*	+2,200	+1,700	+75.3	+87.2	+162.5	+58.1	+113.0	−54.9
10*QL*	+320	−180	+21.6	−17.0	+4.6	−12.2	+30.2	−42.4
11*PQ*	+410	+270	+33.7	+59.1	+92.8	+22.2	+90.0	−67.8
12*SP*	+1,250	+800	+41.6	+35.0	+76.6	+26.6	+54.8	−28.2
13*SN*	+1,280	+1,020	+52.0	+59.0	+111.0	+47.6	+74.0	−26.4
14*NM*	+780	+770	+40.6	+47.5	+88.1	+40.0	+48.2	−8.2
15*IM*	+360	0	+33.9	0.0	+33.9	0	+48.2	−48.2
16*IJ*	+160	−270	+10.4	−27.3	−16.9	−17.6	+16.2	−33.8
17*OJ*	+240	+330	+57.6	+105.8	+163.4	+79.1	+76.9	+2.2
18*PO*	+370	+390	+114.1	+180.2	+294.3	+120.4	+170.8	−50.4
19*KP*	+540	+360	+24.5	+45.6	+70.1	+16.3	+68.5	−52.2
20*TP*	+2,780	+4,030	+24.9	+106.5	+131.4	+36.0	+73.4	−37.4
21*UP*	+1,470	+1,610	+66.1	+143.4	+209.5	+72.5	+131.0	−58.5
22*TU*	+1,310	+2,420	+26.2	+145.0	+171.2	+48.4	+78.6	−30.2
23*AI*	+680	+780	+16.6	+171.0	+187.6	+19.0	+149.0	−130.0
24*HC*	+120	−1,290	+1.8	−159.0	−157.2	−19.9	+14.8	−34.7

this type, there is some modification of the connections between the network and the input and output busses for the I_q current setup due to the fact that loads with leading or positive reactive power are set up as generators of negative reactive power. In this problem this change is necessary for the connections to busses H and I. The reason why the voltage differences between all busses and reference bus P are designated as $-I_{qx}X$ voltages is that $+I_q$ currents are made to flow in at the input bus and out at the output bus, whereas actually they should be set up as $-I_q$ currents to conform to the signs of Table II.

Similarly by using the R network and $+I_q$ currents the $-I_{qr}R$ voltage differences are obtained.

Referring again to Table III, the inphase voltage difference between all busses and the reference bus P is $V_p=I_{pr}R-I_{qx}X$ for each case, and the quadrature component of voltage difference is $V_q=I_{px}X+I_{qr}R$.

5. Computation of Components of Current Flowing in Each Circuit of the Network

Referring to Table IV, the voltage differences between the bus terminals of each circuit are derived from the V_p and V_q values of Table III. For example, V_p for circuit 1*BA* is obtained by subtracting V_p for *AP* from V_p for *BP* in Table III which gives $+460-230=+230$ volts. In like manner, V_p and V_q for all circuits of the network are derived.

Next, as shown in Table IV, the equations $I_p=GV_p+BV_q$ and $I_q=GV_q-BV_p$ are used to determine the inphase and quadrature components of current which must flow in each of the circuits of the actual a-c network to give the V_p and V_q voltage differences derived from the d-c board setups. The values of conductance G and susceptance B are those shown in Table I for each circuit of the network.

6. Computation of Bus Voltages and Real and Reactive Power

As noted at the start of the solution, the reference bus P was established as having a voltage to neutral of $33.33+j0$ kv. From Table III the voltage differences to neutral of components V_p and V_q were obtained between all busses and bus P. To determine the voltage to neutral at every other bus, these voltage differences are added algebraically to the reference voltage. This is done, and the results are tabulated as E_p and E_q in Table V.

The current components I_p and I_q in Table V are not exactly the same as the current components given in Table II which were the basis of the d-c board setup, but instead are the algebraic sum of the components of current away from each generator bus and toward each load bus, so that Kirchhoff's law will be satisfied. For example at generator bus A the actual input current is equal to the sum of currents 1*AB* and 23*AI* expressed in their components. From Table IV, I_p for 1*AB* is −107.0 amperes, and I_p for 23*AI* is +187.6 amperes, and the algebraic sum is +80.6 amperes as shown in Table V. All I_p and I_q current components given in Table V are obtained in this manner.

The computations of real and reactive power in megawatts and megavars are obtained by using equations megawatts $=0.003\ (E_pI_p+E_qI_q)$ and megavars $=0.003\ (E_pI_q-E_qI_p)$ as shown in Table V.

Table VI is added to show the computation of the phase voltages at all busses and to give a direct comparison of the derived values of real and reactive power, which completely satisfy the current distribution of Tables IV and V and the voltages of Tables V and VI, with the problem setup values of Table II.

The comparison shows that for all

Table V. Network Problem—Computation of Real and Reactive Power

Bus	E_p Kv	E_q Kv	I_p Amperes	I_q Amperes	Real Power in Megawatts $=0.003(E_pI_p+E_qI_q)$	Reactive Power in Megavars $=0.003(E_pI_q-E_qI_p)$
A	33.56	−0 21	80 6	−42.0	8.16	−4.18
B	33.79	−0.04	387.8	−70.6	39.31	−7.09
K	33.87	+0.36	243.5	−75.3	*28.33	*−9.05
S	34.58	+0.80	187.6	−54.6	19.37	−6.12
T	36.11	+4.03	302.6	−67.6	31.97	−10.98
U	34.80	+1.61	299.8	−62.5	30.99	−7.97
C	33.57	−1.04	123.6	−17.3	12.53	−1.36
D	32.41	−1.21	120.9	−50.9	11.95	−4.50
E	32.64	−0.75	94.3	−31.9	*9.33	*−2.92
F	33.16	−0 34	−7.2	−18 6	*(−0.70)	*(−1.86)
G	32.72	−1.95	69.0	−41.6	*7.02	*−3.68
H	33.69	−2 33	193 4	+136.0	18.58	+15.12
I	32.88	−0.99	78.1	+11.7	7.67	+1.39
J	32.72	−0.72	146.5	−31.6	14.47	−2.80
L	32.60	−0.09	167.1	−97.3	16.35	−9.46
M	32.52	−0.99	122.0	−56.4	12.09	−5.16
N	33.30	−0.22	22.9	−18.2	2.30	−1.81
O	32.96	−0.39	130.9	−52.6	13.02	−5.05
P	33.33	0.0	100.5	−58.9	10.05	−5.88
Q	32.92	−0.27	88.2	−25.4	8.75	−2.44
R	33.62	−1.11	51.7	−20.4	5.28	−1.89

* Bus *F* was not set up as a load so that the real and reactive power at *F* may be moved to busses *K*, *E*, and *G* in amounts proportional to the admittance of the circuits between *F* and its adjoining busses. This is done in Table VI.

practical purposes the first solution is adequate for the analysis of this network problem. The greatest difference between derived and setup values of real and reactive power occurs at load buses *O* and *P* which are connected by a very low impedance circuit of $(1+j1.5)$ ohms. If the two loads are added together, the derived total is $23.07-j10.93$ megavolt-amperes as compared to a setup total of $23.0-j9.0$ so that the effect of the differences of the two loads would be negligible in the network. However, in case greater accuracy is desired, it is possible to repeat the solution with corrected values of I_p and I_q to obtain derived values of real and reactive power which check almost exactly with the setup values. The method of correcting the I_p and I_q currents is described in the section which follows, but the solution is not repeated, because it would add nothing of value to the paper.

Procedure for Second Solution

Unless a particularly accurate second solution were desired, the values of real and reactive power tabulated in Table VI would be compared, and the difference in the derived and setup values would form the basis for estimating the changes in I_p and I_q which would result in improved accuracy. However, a more exact procedure is given in the following example.

At bus *O* from Table II the load setup was $16.0-j6.0$ megavolt-amperes instead of the derived value of $13.02-j5.05$ megavolt-amperes, which satisfied the current distribution and voltages of the first solution. The voltage components for bus *O* from Table V are $E_p=+32.96$ and $E_q=-0.39$, and since these voltage components will not change appreciably in the second solution, the values of I_p and I_q can be computed with the equations:

$$I_p=(E_pP-E_qQ)\ 1{,}000/(E_p^2+E_q^2)3$$

$$I_q=(E_qP+E_pQ)\ 1{,}000/(E_p^2+E_q^2)3$$

In these equations *P* and *Q* are in megawatts and megavars and E_p and E_q are in kilovolts to neutral.

Whence $I_p=160.6$ amperes and $I_q=-62.5$ amperes.

In Table II the current components to the load at bus *O* were set up $I_p=160$ and $I_q=-60$; however in Table V the derived values were $I_p=130.9$ and $I_q=-52.6$. This difference is due to variations in X/R ratios in the circuits of the network, and about the same difference would occur in the second solution. Therefore, a corrected value of $I_p=160.6$ $+(160-130.9)=189.7$ would be used for the second solution. The corrected value of $I_q=-62.5+(-60+52.6)=-69.9$ would also be used for the second solution. This same procedure could be followed at all other busses of the network to obtain corrected values for a second and more accurate solution to the problem.

Application of Superposed Method of Setup to the D-C Board

In cases where the d-c board does not have a sufficient number of rheostats to set up all of the generators and loads, it is possible to set up any proportion of the generators and loads at one time, so long as the total input current of the several generators adds up to the total output current to the several loads. The same voltage measurements from all busses to the reference bus would be required for each such setup, and the sum of the voltages thus obtained would give the same result as though all generators and loads had been included in one setup. This method requires additional readings, but because of the smaller number of generator and load rheostats which must be adjusted to obtain correct input and load currents, the current adjustment is simplified.

Use of Method With D-C Board With Fixed Resistors

If a d-c board is of the fixed resistor type, it may be possible to adapt it for use with this method, provided it has enough rheostats to use for generator and load-current adjustment.

Most networks have many circuits with X/R ratio almost equal. If the board is set up to simulate *X* only, this network is already available. Assume that 75 per cent of the circuits of a particular network have an X/R ratio of 2/1 or approximately so, and that only 25 per cent vary much from this ratio. If a means is provided for changing this 25 per cent to have the 2/1 ratio for setups requiring the *R* network, then the method may be used. The board constant for the *R* network would then be one-half that of the *X* network.

Use of Method Where Line Capacitance Must Be Considered

In the network problem of this paper, the effect of shunt capacitance of the lines was neglected, because the amount of leading megavars of the circuits would not change the solution appreciably. However, if it is necessary to consider line capacitance, it is suggested that the capacitance + megavars for each line be computed on the basis of estimated voltages at the start of the problem. In the usual manner one half of the megavars of all lines terminating at a bus would be allocated at that bus, and an adjustment in net load or generator megavars at that bus would be made to obtain the corrected megavars from which the I_q component of current would be computed.

Table VI. Network Problem—Computation of Phase Voltage at Busses; Comparison of Result of Table V With Values Set Up in Table II

Bus	E_ϕ in Kv $=1.732\sqrt{E_p^2+E_q^2}$	Loads* Megawatts	Loads* Megavars	Generation* Megawatts	Generation* Megavars
A	**109.3	— —	— —	8.16 (8.0)	−4.18 (−5.0)
B	**110.0	— —	— —	39.31 (38.0)	−7.09 (−9.0)
C	**109.4	12.53 (12.0)	−1.36 (−1.8)	— —	— —
D	56.3	11.95 (12.0)	−4.50 (−6.0)	— —	— —
E	56.6	8.99 (9.0)	−3.82 (−4.0)	— —	— —
F	57.5	— —	— —	— —	— —
G	56.8	6.96 (7.0)	−3.84 (−4.0)	— —	— —
H	58.6	18.58 (21.0)	— —	— —	−15.12 (−16.0)
I	57.0	7.67 (6.2)	— —	— —	−1.39 (−1.4)
J	56.8	14.47 (14.0)	−2.80 (−3.0)	— —	— —
K	58.7	— —	— —	28.63 (24.5)	−8.25 (−6.5)
L	56.5	16.35 (17.0)	−9.46 (−10.0)	— —	— —
M	56.4	12.09 (11.8)	−5.16 (−5.0)	— —	— —
N	57.7	2.30 (3.0)	−1.81 (−2.0)	— —	— —
O	57.2	13.02 (16.0)	−5.05 (−6.0)	— —	— —
P	57.7	10.05 (7.0)	−5.88 (−3.0)	— —	— —
Q	57.1	8.75 (9.0)	−2.44 (−3.0)	— —	— —
R	58.4	5.28 (5.0)	−1.89 (−2.0)	— —	— —
S	60.1	— —	— —	19.37 (20.5)	−6.12 (−6.5)
T	62.9	— —	— —	31.97 (33.0)	−10.98 (−9.0)
U	60.3	— —	— —	30.99 (33.0)	−7.97 (−7.0)
Totals	—	148.99	−48 01	158.43	−61.10

* Problem values set up in Table II are given in brackets.

** Turns ratio of 115/57-kv transformers = 1.88/1.

Setup of Transformers With Different Ratios in a Loop Circuit

In a paper presented before the Portland section of the AIEE, the writer included an example to show how a series boost or buck may be provided from a separate source of direct current inserted at the point of boost or buck in the loop circuit, but rather than lengthen this paper, the method will be indicated only.

In most networks the quadrature component of voltage with respect to the reference voltage is small as compared to the inphase component, so that the effect of any normal boost or buck on the E_q component is negligible. However, if one transformer has a ten per cent boost or buck with respect to other transformers operating in closed paths with this transformer of different ratio, then a separate d-c source should be inserted at the point of boost or buck in the X network setup with I_q current flowing. This gives the desired effect to the inphase voltage readings for V_p. In the problem of this paper the voltage to neutral is approximately 33 kv at bus I, so if a boost of ten per cent were required in transformers 23AI, the series potential required would be 3,300 divided by the board constant. It would be inserted between bus A and the resistance unit representing X of the transformers 23AI. If the potential source had appreciable internal resistance, the 23AI unit setting would be reduced by this amount. As noted before, it would be used only in the setup for reading $I_{qx}X$ voltage differences.

Setup Using Ammeter Instead of Voltmeter on D-C Board

Although the measurement of the $I_{pr}R$, $I_{px}X$, $I_{qr}R$, and $I_{qx}X$ voltages between all busses and the reference bus is recommended, it is possible to read current distribution in each of the four network setups on the d-c board and compute the voltage differences for all circuits in each setup, and obtain the same results.

Conclusions

The scope of this paper has been quite limited with respect to the broad field of a-c network analysis, but it is believed that the network problem and the discussion show how this new method can be applied with the aid of a conventional d-c board to solve complex problems of load distribution and voltage conditions for a-c networks in a relatively simple manner and with good speed.

In the application of the method to the conventional d-c board, it probably will become apparent that various combinations of resistance networks could be set up simultaneously, which, together with proper metering arrangements could be used to reduce the analysis from four board setups to one—thus eliminating the greater part of the computation work. Patent application has been made for the method as applied to such simplifying resistance network combinations.

As an indication of the time required for a study of this type, the writer unaided set up the board and took the four sets of readings required in eight hours. An additional six hours was required to make all of the computations and tabulate the results. If the outline of procedure given in the paper is followed, it is believed that other engineers will find the method to be a useful tool in their system planning work.

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3. Electrical Characteristics of Transmission Circuits (third edition, 1926), William Nesbit. Paralleling of Transmission Circuits. Pages 183–4.

4. Load Studies on the D-C Calculating Table, W. C. Hahn. *General Electric Review*, part 1, July 1931, page 444; part II, August 1931, page 482. (This is very complete and shows how the d-c board may be used for load studies, but it follows an entirely different approach to the problem of d-c board setup than that given in the present paper.)

Design, Manufacture, and Installation of 120-Kv Oil-Filled Cables in Canada

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Synopsis: This paper describes the 120-kv oil-filled cable system of the Montreal Light, Heat, and Power Consolidated, installed during 1941 and in present operation as a link in a large interconnection scheme.

The paper describes the functions of the various sections of the system and the reasons for the choice of this method of effecting the interconnection. The steps required in designing the cable system are outlined and the general principles governing each such step discussed.

A short section deals with manufacture followed by a section covering the organization required for installation and the methods followed. The final section deals with operating practice, especially the basis of loading.

System

THE 120-kv oil-filled cable system of the Montreal Light, Heat, and Power Consolidated as at present installed may properly be divided into the following sections: (Figure 1):

1. Station *A* to station *B*—two circuits
2. Station *B* transformer cables—two circuits
3. Station *B* to station C—one circuit

The first and third parts are much the most important in size, but the second part, while relatively small, presents a use of high-voltage cable of particular interest to transformer station engineers.

In sequence of installation one circuit plus a spare phase of section 1 were purchased and installed initially on one contract, the remaining two phases of that section and all of sections 3 on a second contract. Section 2 was on a third contract but was installed during the same period as section 3.

Section 1 (known as circuits *A* and *B*), takes power from the 110-kv bus of station *A* directly to the new station *B* for service to an important business and industrial section of the city. Station *B* departs from the general substation scheme of the utility, in that it is not part of their 60-kv ring system, but is fed practically direct from the generating source, station *A* acting as a switching station. The present availability of underground cable suitable for 110-kv operation permitted locating the new station at its proper load center without double transformation or further loading or reinforcement of the already busy 60-kv ring. It relieves the ring as well and permits further load growth with existing facilities.

Section 2 takes power from the 110-kv bus of station *B* to the transformers, at present two, with provision for an ultimate of four. Underground feed was chosen as providing a substantial saving in steel structure, greater compactness and better appearance and much less exposed high-voltage circuit. Also when combined with underground cable on the low-voltage side, it permits a virtually "dead-front" station, excepting under the easily isolated and relatively small high-voltage bus section which lies in one 25-foot-wide strip along the south side of the station property.

Section 3 (known as circuit *C*), takes power from the 110-kv bus of station *B* to station *C* and acts in combination with circuits *A* and *B* as a direct tie between the Beauharnois system of the Montreal Light, Heat, and Power Consolidated supplying station *A* and the system of the Shawinigan Water and Power Company, and through it to the Saguenay Power Company system. It also will serve as an additional circuit to station *B*.

This direct tie between the first two systems was made in order to place the entire generating capacities of these three large companies on one interconnection, from which power for wartime industrial requirements could be drawn by the industry most vitally needing it.

The Shawinigan and Saguenay systems were already connected together by high-voltage transmission lines with ample capacity to carry any loads expected. Between the Beauharnois and Shawinigan systems the connection consisted of a 60-kv double ring tied through transformers to the 110-kv system of the Shawinigan Company and through other transformers to the 110-kv Beauharnois system (Figure 2). Thus the power that could be transferred from the Beauharnois system to the interconnection of the Shawinigan and Saguenay systems was limited by the Montreal Light, Heat, and Power Consolidated 60-kv ring and transformer capacity which was partially used by the primary power requirements of the Montreal Light, Heat, and Power Consolidated system.

The original suggestion was to construct an overhead 110-kv tie around the metropolitan area of Montreal between the Beauharnois system and the Shawinigan system. This would have involved the building of about 15 miles of steel tower transmission line over territory where the right-of-way problems were difficult and uncertain, especially as to the cost and time required to negotiate.

The second suggestion was to extend the 120-kv oil-filled cable system then nearing completion under the first contract mentioned above (circuit *A* of section 1) to tie with the 110-kv transmission system of the Shawinigan Company. This required the adding of two phases between stations *A* and *B*, which combined with the spare phase already installed for circuit *A* to become circuit *B* plus about five miles of additional cable circuit across the metropolitan area.

The cable tie was selected on the basis of several factors, most important among which were:

Initial cost
Time required
Stability
Freedom from lightning troubles
Present and future public reaction

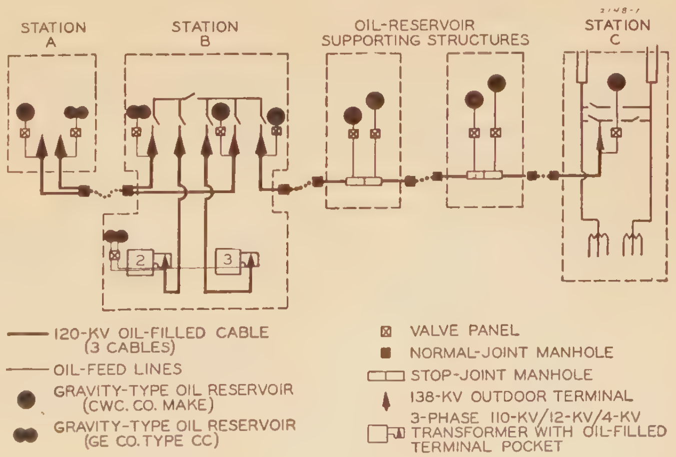

Figure 1. Single-line diagram of system

Paper 42-148, recommended by the AIEE committee on power transmission and distribution for presentation at the AIEE Pacific Coast convention, Vancouver, British Columbia, Canada, September 9–11, 1942. Manuscript submitted June 1, 1942; made available for printing July 8, 1942.

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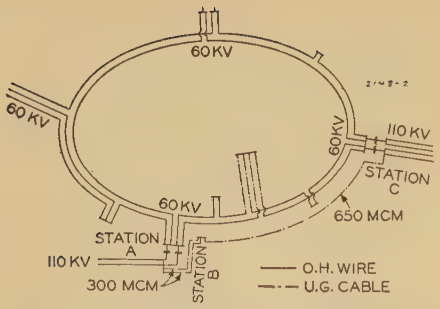

Figure 2. Interconnections between Montreal Light, Heat, and Power Consolidated and Shawinigan Water and Power Company systems

Since these three contracts constitute the most important underground cable installation in Canada and its highest-voltage underground system (the previously highest being 66-kv cables constituting part of the above 60-kv ring installed during 1927, 1931, and 1937 by the suppliers of the present cable), and since the type of cable is new to Canada, descriptions of the elements of an oil-filled cable system and the problems and steps in its design and installation seem warranted.

Cable-System Design

Underground cable systems above 75 kv fall into classifications of:

(*a*). Oil-filled cables in which practically all gas is removed from the cable, insulation, impregnant, joints, and terminals, ensuring that none of these elements are permitted to come into contact with a gas thereafter and so engineering the system that no part of it will be subjected to pressures less than atmospheric during service. It necessitates the use of an oil-impregnant fluid at all operating temperatures and oil channels giving access for the impregnant to all parts of the cable system.

(*b*). High-gas-pressure systems[1,2] in which ionization of gas pockets in the insulation is prevented by keeping them at such high gas pressures that their critical corona voltage substantially exceeds any electric stress gradients in the insulation. The usual pressures are 200 to 250 pounds per square inch. There are several such installations in service up to the 132-kv range but mostly at 66 kv. The pressures employed demand the use of steel as a containing element which gives the added advantage of excellent mechanical protection but requires great care in protection against corrosion, a serious problem where buried in earth over relatively long distances.

As the oil-filled class of system is well adapted to installation in conventional duct systems in busy city streets, and as there is in excess of 1,200 miles in service throughout the world at voltages up to 220 kv, this was the type chosen for this cable system. It is the type dealt with hereafter in this paper.

The design of an oil-filled cable system involves the following elements of choice, design and engineering:

1. Type of installation, that is, duct or buried directly in earth.
2. Number of conductors per cable.
3. Size of oil channel.
4. Size of conductor.
5. Thickness of insulation.
6. Thickness of sheath.
7. Spacing between oil-feed points.
8. Type of oil feed, that is, gravity or balanced pressure.
9. Normal joint design.
10. Pothead design.
11. Stop-joint design.
12. Size of oil-feed pipe lines.

1. Ducts

As the cable system lies beneath the streets of one of the largest cities of Canada and under some of the busiest streets of that city, the well-accepted practice of installing the cable in a duct system was the choice. Diameters for the 300,000-circular-mil and 650,000-circular-mil cables were approximately $2^1/_4$ and $2^1/_2$ inches respectively, for which $3^1/_2$ inch and 4 inch fiber ducts were chosen.

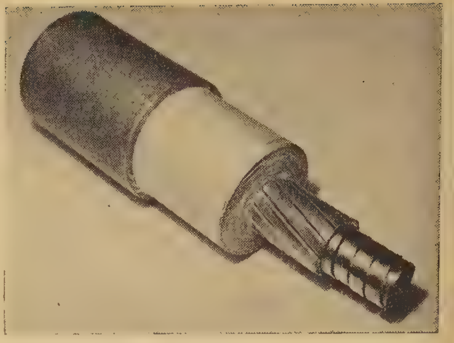

Figure 3. Cable construction

The duct-line route was selected for the most level contour (Figure 14), always desirable for oil-filled cable. Several surveys were made to determine such a route, which fortunately was one of the most direct, although through one of the busiest sections of the city.

The total length of the conduit run was approximately 36,000 feet. Work was commenced for the ducts for the second phase of the work, approximately 125,000 duct feet, in early June 1941 and completed by the end of September of the same year.

All manholes were built in such a way as to allow a minimum horizontal cable offset between duct mouth and joint of 42 inches. This was based on recom-

Table I

Circuits	Station A to B	Station B to C
Operating voltage (for calculations)	120 kv	
Frequency	60 cycles	
Number loaded cables per duct bank	6	3
Insulation thickness*	0.500 inch	0.500 inch
Lead thickness (for calculations)	0.125 inch	0.133 inch
Duct centers (for calculations)	$5^1/_4$ inches	$6^1/_8$ inches
Duct configuration	*A B C* / *C B A*	*C* / *B A*
Maximum daily load factor	75 per cent	
Maximum daily loss factor	$62^1/_2$ per cent	
Maximum ambient temperature of earth	20 degrees centigrade	
Maximum permissible copper temperature	70 degrees centigrade	
Duct thermal constant	1.04	
Thermal resistivity of insulation	550 ohms	
Power factor of insulation at 70 degrees centigrade	0.75 per cent (guaranteed value)	
Specific inductive capacity of insulation	3.7	
Sheath currents	Flowing	Not flowing
Current amperes	375	600
Conductor area circular mils	300,000	650,000

* See Table II.

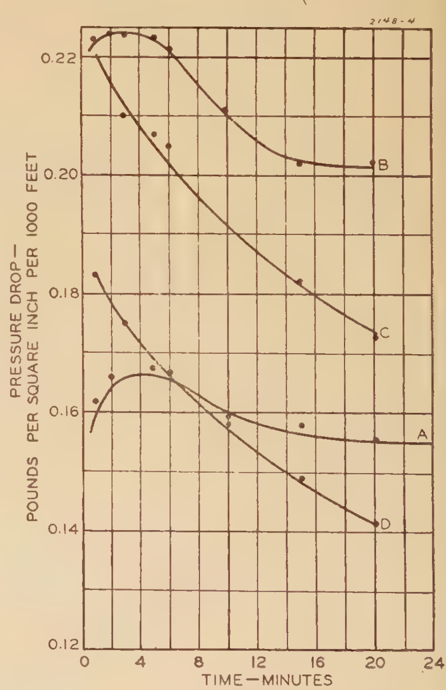

Figure 4. Curves of transient pressure drop per 1,000 feet versus time, due to dropping load and dielectric loss

Curve *A*—From full load
Curve *B*—From 75 per cent load
Curve *C*—From 40 per cent load
Curve *D*—From 25 per cent load

mendations of Herman Halperin of Chicago, whose advice on this and other features is gratefully acknowledged.

Records of cable pulls on section 1 indicated that for straight runs a coefficient of friction of about 0.25 to 0.3 could be used for calculating cable pulls. On this basis it was decided to attempt to locate manholes up to 1,000 feet apart. As constructed, the maximum length is 975 feet, 8 inches, and the average length for section 3 is 814 feet. For this reason particularly rigid inspection of ducts was made to eliminate all bad or doubtful pieces of ducts or couplings. The duct supply, however, was found to be extraordinarily good.

Ducts were laid in a concrete envelope and in a manner to provide drainage to manholes. An attempt was made to maintain a 30-inch cover at least. In all but a very few cases the manholes have drainage connections to the city sewer system. In consequence, very little time had to be spent pumping water.

One section of duct run extends under a ship canal. Here the duct mass has a 6-foot cover to the bottom of the canal. Considerable care was taken in this construction to insure the best reinforced concrete job possible, not only because it passed under a ship canal, but further because the bottom on which the mass was constructed was quicksand. This section was built of 3,000-pound concrete reinforced with steel rods. The slump of the concrete mix was watched especially carefully.

2. Number of Conductors

Cable dimensions practically preclude the use of multiconductor cables at 120 kv. Three single-conductor cables per circuit consequently were the necessary decision, and design and calculations were based accordingly.

3. Oil Channels

There are in common use for single-conductor cables, two oil channel sizes, namely 0.500 inch and 0.690 inch.

The smaller diameter core results in a substantial saving in cable cost, but the larger core permits feeding nearly twice as far as the smaller core. The use of the smaller core increases substantially manhole, joint, reservoir, and miscellaneous accessory and installation costs. At the same time it restricts the choice of the type of reservoir and adds to the number of points requiring operating supervision and inspection.

It was decided to employ the 0.690-inch oil channel, especially since section 1 was the only part originally contemplated, and, as it was 8,800 feet long, this permitted its being fed from the ends alone.

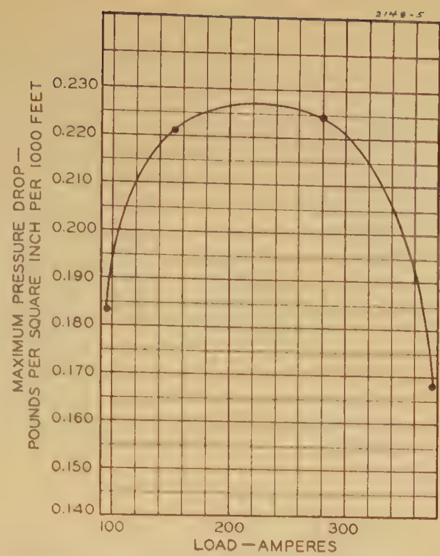

Figure 5. Curves of maximum transient pressure drops, versus load prior to interruption

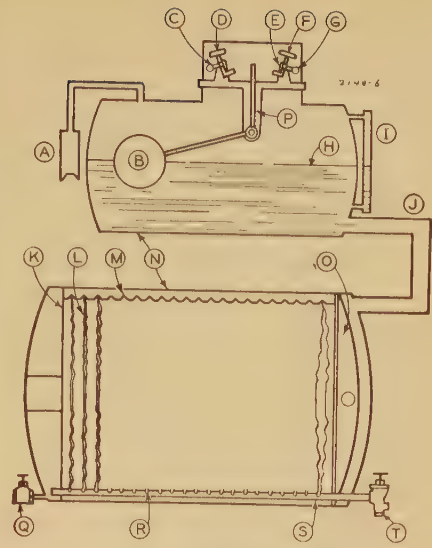

Figure 6. Schematic diagram of new gravity-type oil reservoir (Canadian design)

A. Activated alumina dehydrator
B. Float
C. Oil-level relay
D. Low level
E. High level
F. Mercoid switch
G. Counterweight
H. Gauge oil level
I. Oil-level indicating gauge
J. Pipe-connecting tanks
K. End plate for cell unit
L. Oil cells
M. Cell spacers
N. Galvanized steel tank
O. End plate for cell unit
P. Actuating arm
Q. Gauge oil drain valve
R. Cell oil pipes
S. Stainless-steel tube manifold
T. Packless diaphragm valve

4. Size of Conductor

The size of conductor was determined by accepted methods[3] based on thermal limitations. Bases for calculations were as set forth in Table I.

At the time of designing the system as far as station *B*, generous factors of safety for a single circuit determined the choice of 300,000-circular-mil conductors. The addition of the second circuit brought the capacity of this part of the system to approximately 140,000 kva.

A load of about 30,000 kva is expected at station *B* during the war period, leaving an available capacity of 110,000 kva or more to pass on to station *C*. It was therefore decided to continue the tie line for this capacity, which gave a cable size of 650,000 circular mils as indicated by Table I, the load and loss factors having been determined as follows:

The shape of the cable load curve was the Montreal, Light, Heat, and Power Consolidated's system load curve inverted and superimposed on a flat load curve whose value was the difference between the system peak and its generating capacity. This gave a load factor less than 75 per cent and a loss factor of above 50 per cent. Such being the case, it was decided to make all calculations on the more or less standard basis of 75 per cent load factor and $62^1/_2$ per cent loss factor, which were expected would provide the additional capacity required to carry weekend surplus power.

5. Thickness of Insulation

As other system characteristics precluded a higher voltage than 120 kv ever being used, an insulation thickness of 0.500 inch was chosen.[4]

Table II. Cable Dimensions

		300,000 Circular Mils	650,000 Circular Mils
Steel channel	Inside diameter (inches)	0.69	0.69
	Outside diameter (inches)	0.79	0.79
Copper conductor	Outside diameter (inches)	0.98	1.17
Paper insulation	Thickness (inches)	0.50	0.50
	Outside diameter (inches)	1.98	2.17
Lead	Thickness (inches)	0.125–0.156	0.133–0.164
	Outside diameter (inches)	2.23 –2.29	2.44 –2.50
Weight per foot	Pounds	7.15 –8.46	8.16 –9.40

6. Thickness of Sheath

After consultation with several authorities on stresses permissible in lead cable sheaths (a lead sheath containing approximately 0.06 per cent of copper was used), it was decided not to exceed a fiber stress in the sheath under static conditions of 125 pound per square inch.

The elevation of the oil reservoirs having been determined by considerations dealt with in the next part of this paper, the actual cable profile showed that internal oil pressures would be such that in some sections the cable sheaths would require to be increased. Sheath thicknesses for the 300,000-circular-mil cable varied from 0.125 inch to 0.156 inch and for the 650,000-circular-mil cable from 0.133 inch to 0.164 inch.

Figure 3 illustrates the cable construction.

7. Spacing Between Oil-Feed Points

The making of this decision involved some of the most extensive and interesting calculations of the entire design. The governing consideration is that on dropping load the cable cools and demands oil. During this cooling period a "transient" drop in oil pressure occurs at all points away from the oil supply and is greatest at the point most remote from supply, that is, halfway between feed points.

It is a standard of oil-filled cable system design that no point in the system shall fall below one pound per square inch above atmospheric pressure (in order to prevent ingress of air or moisture if the cable sheath be punctured). When the "transient" drop for unit distance under worst conditions is known and correlated with the pressure head permissible without overstressing the sheath (which sets the reservoir elevation or pressure), the maximum feeding distance is determined.

Steps in determining the "transient" are, in brief:[5]

(*a*). Determination of the thermal capacitances of the cable elements.

(*b*). Determination of the thermal resistances of the cable elements.

(*c*). Determination of the rate of cooling of each element from representative initial conditions such as full load, 75 per cent, 50 per cent, and 25 per cent loads at minimum earth ambients (assumed 0 degrees centigrade).

(*d*). Determination of oil-volume demand (proportional to first power of *c* above).

(*e*). Determination of oil temperatures at each instant and the viscosities of the oil at these temperatures.

(*f*). Determination from *d*, *e*, and fluid friction coefficient of oil channel, of the transient drop for a unit length at successive intervals for each initial loading condition.

Curves of pressure drop against time for dropping from various loads are shown in Figure 4. It will be observed that maximum drop occurs as late as three minutes after zero time.

Figure 5 of maximum pressure drop versus initial loads is then drawn from the maximum values obtained from the curves of Figure 4. It will be seen that worst conditions obtain in dropping about one-half to two-thirds full load during winter conditions.

As pressure heads in the order of 15 pounds per square inch are quite safe for normal lead sheaths of these diameters, it was felt that there were ample margins of safety in using feeding distance of up to 5,000 feet, that is, spacing between feed points of 10,000 feet. This proved suitable for conditions in the field, as station *B* is 8,800 circuit feet from section *A*, and there were suitable locations for oil-reservoir structures at approximately 9,000 feet and 18,000 feet from station *B*, and station *C* lies about 27,000 feet from station *B*.

8. Type of Oil Feed

Two general types of oil reservoirs are available to the design engineer.

(*a*). *Gravity type* in which the requisite pressure to reach all the system is obtained by locating the reservoir at an elevation determined by the cable profile and the feeding distance, as the oil in the reservoir is at atmospheric pressure (plus the slight back pressure of the cell walls). In practically all cases, excepting for unusual profiles, gravity reservoirs must be mounted above the ground surface and cannot be used unless above-surface space is available at the feed points.

(*b*). *Balanced Pressure type* in which the requisite pressure is obtained by gas confined within the reservoir shell (plus auxiliary gas tanks if necessary) and external to the oil cells. The use of such reservoirs requires careful gas volume calculations and adjustments and introduces certain extra operating and maintenance items. However, they can be mounted in manholes and must be used when above-surface reservoir space is not available.

In the systems covered by this paper above-surface space fortunately was available adjacent to desired feeding points. The gravity type of oil feed was chosen.

Figure 6 illustrates one type of gravity-type reservoir employed. This reservoir is a new development not previously used. It was designed and manufactured in Canada and has particularly low dead oil volume and cell back pressure. It is of 36 gallons (U. S.) capacity between plus 0.5 pound per square inch and minus 0.1

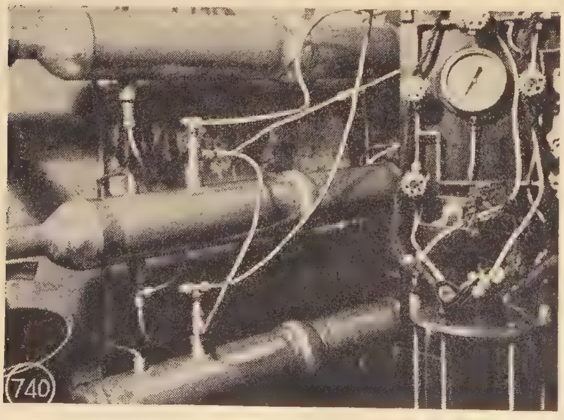

Figure 7 (left). Gang-treating normal joints

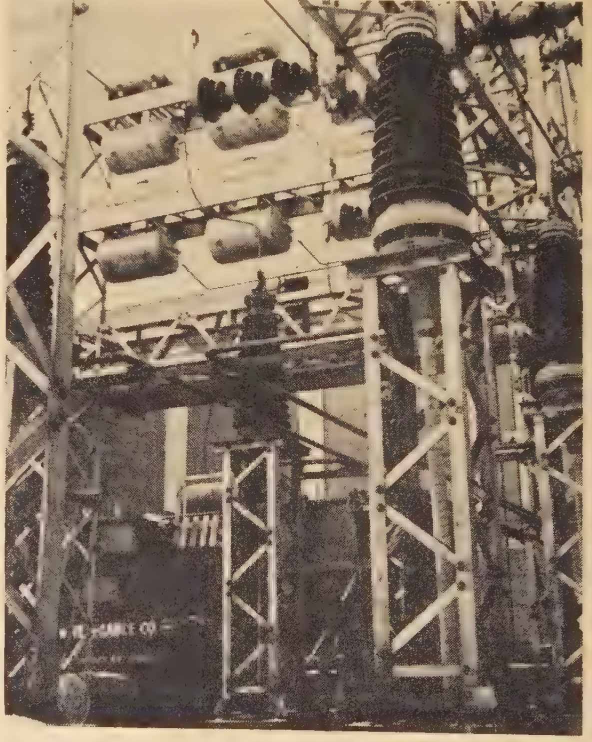

Figure 8 (right). Station *B* potheads, oil reservoirs, and oil pipe lines

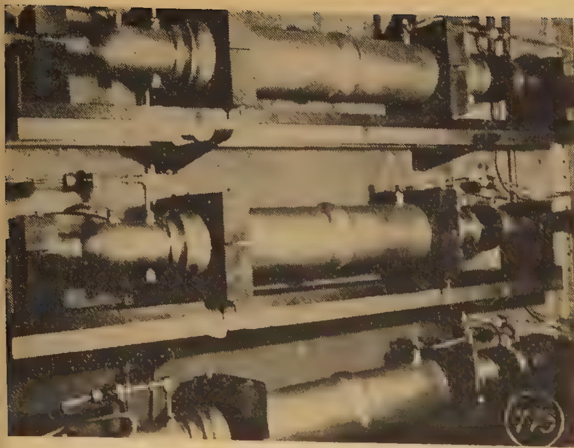

Figure 9 (left). Stop joints, cradles, and piping (manhole 1040)

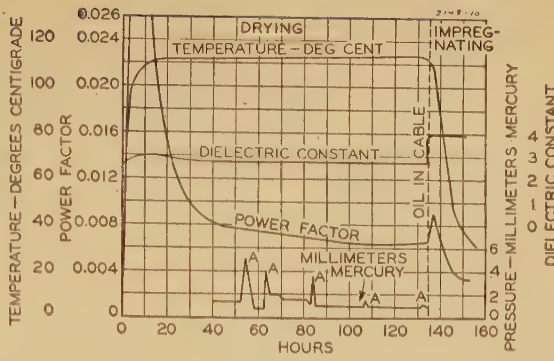

Figure 10 (right). Evacuation and impregnation curves

pound per square inch cell pressures. The smaller tank acts as a riser for the gauge oil to permit keeping the oil cells of the main reservoir completely immersed at all times and out of contact with the air.

The low dead oil capacity is due to both sides of each cell nesting together. Tests of numerous individual cells gave a life of 1,500 cycles not to failure. Each cycle was the full yearly travel of the cell. The daily working would not be 25 per cent of this amount.

9. Normal Joint

Normal joint design is similar to that for high-voltage solid-type cable with provision by means of hollow cores, three way valves, and oil ports for flushing out of any cable oil overheated during the soldering operations and establishing a permanent oil channel through the connector for servicing cable sections lying beyond the joint. Sharp depressions in the connector body which might set up high electric stresses were filled in by lead discs cast on the job and dressed carefully to the contour of the adjacent surface.

The initial joints were imported from the United States. The later ones were made in Canada (see Figure 7). Both were of designs in common use in the United States.

10. Pothead Design

Pothead design requires provision for relieving end stresses such as by combination of stress cones and barrier tubes, access to the cable core for oil to enter and leave the cable, and means of shutting off the core of the cable from the pothead body while erecting, evacuating, and filling the pothead. Two designs in common use in the United States were employed. On account of the low temperatures to be anticipated in the district, extra large oil ports were provided in the connector fittings. Here again part were manufactured in United States, but the later and larger quantities were made in Canada (see Figure 8).

11. Stop Joints

Stop joints serve two purposes. One is to segregate the oil systems where contours require that one section, having for example a higher elevation than an adjoining section, needs its reservoirs mounted at a higher elevation. At the same time troubles in one section are not communicated to adjacent sections. The second main function is that these joints act as a feed point and provide means of ingress and egress for the oil in the cable core. In the design utilized the stop joint essentially is a pair of cable terminals placed end to end, joined by a suitable connector block, and enclosed within a copper sleeve. Reinforcement insulation and enclosure within oil permits these terminals to be quite compact and readily mounted within a manhole. These were imported from United States.

Oil entry fittings are provided at each end of the joint to provide independent oil feeds to the cable sections on both sides of the joint. Impregnated wooden cradles support these long joints (see Figure 9).

12. Size of Oil-Feed Pipe Lines

Due to the very low temperatures likely to be encountered in the locality, and acting on the recommendation of Herman Halperin, it was decided to make exposed oil-feed lines from the reservoirs 1.5-inch copper pipe (Figure 8). Those parts of the oil-feed lines to the stop joints between the valve panel (where it is close to the foot of the structure) (Figure 9) and the stop joints were of one-inch soft copper pipe pulled into fiber ducts.

Manufacture

The manufacture of these oil-filled cables, although only slightly more complex, was different from the manufacture of solid-type cables, chiefly in that drying and impregnating of the paper insulation was done after the core was lead-sheathed. This is possible because of the presence of the oil channel within the conductor.

As may be seen from Figure 3 the conductor is of a novel make-up. Instead of the usual round wires, in single or multiple layers, 12 segments, carefully designed to fit snugly around the steel channel and present a smooth outer periphery, were stranded with a ten-inch right-hand lay.

An advantage for this conductor is that it inherently reduces the electrostatic stresses formed unless remedial measures are used at the crests of wires in conventional stranding.

After insulating, the core was heated at atmospheric pressure for five to six hours, sufficiently long to dry the insulation slightly and loosen the paper tapes so that the possibility of tearing the tapes during leading would be reduced. Leading followed immediately after the preliminary heating. The cable dimensions were as listed in Table II.

Evacuation and impregnation is effected through pipes wiped by fittings to the ends of the cable length, the sheath acting as the vacuum container. A typical impregnation curve with electrical and absolute pressure values is given in Figure 10.

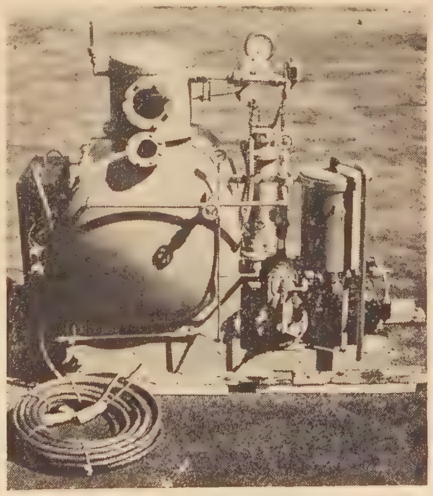

Figure 11. Portable degasser, operating side

Table III. Typical Test Values of Cable

	300,000 Circular Mils	650,000 Circular Mils
Insulation resistance, megohm miles at 15.5 degrees centigrade	6,000	5,000
Conductor resistance, ohms per 1,000 feet at 25 degrees centigrade	0.0355	0.0163
Power factor of insulation at 25 degrees centigrade:		
20 volts per mil	0.0038	0.0031
Working pressure	0.0043	0.0036
190 volts per mil	0.0046	0.0039
Ionization	0.0008	0.0008
Power factor of insulation at working pressure:		
22 degrees centigrade	0.0045	0.0040
80 degrees centigrade	0.0055	0.0048
60 degrees centigrade	0.0040	0.0043
40 degrees centigrade	0.0043	0.0038
Room temperature	0.0044	0.0040
Dielectric watts loss per foot, three-phase, 20 degrees centigrade	0.70	0.70
Dielectric constant at 35 degrees centigrade	3.94	3.94

After cooling, the cables were tested based on Association of Edison Illuminating Companies specifications.[4] Typical results are given in Table III.

Following the tests, the inside end of the cable length was connected through a flexible pipe to degassed oil storage reservoirs housed in the cable drum (Figure 13); then a pulling eye was fitted to the outside end of the length. The pulling eyes were provided with a threaded opening through which oil connections can be made.

With the pulling eye wiped on, the cable was ready for its final washing. A sufficient quantity of oil was washed through the cable core to remove any trace of burnt oil present in the cable because of the wiping operations incidental to fitting the pulling eye. When the power factor of the oil reached a value similar to that of the degasser storage tank oil, and the volume of oil the cable would expel from ten pounds pressure was under the specified limit showing the almost total absence of dissolved gas, the cable was made ready for shipment.

Installation

As this system was sold on an installed basis, the manufacturer was responsible for the cable and accessories until the cable went into operation. Under the manufacturer's engineer in charge, an organization was set up in which a cable-installation company contracted to do the handling, trucking, pulling, and such similar items, to supply top-grade skilled splicers, helpers, and laborers, and to supervise that phase of operation. Great credit is due to this company, as the success and speed with which the work

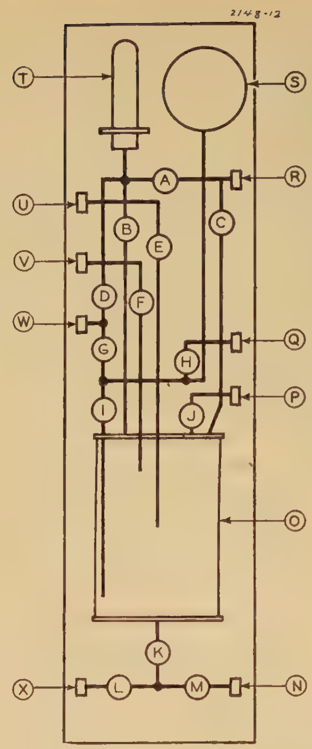

Figure 12. Treating bottle, valves, and connections

Treating panel packless diaphragm valves

A. Jumper vacuum
B. Bottle manometer
C. Bottle vacuum
D. Cable manometer
E. Oil return *A*
F. Oil return *B*
G. Cable oil
H. Oil line
I. Bottle oil
J. CO_2
K. Bottle bottom
L. Joint drain
M. Drain

Inlets and outlets

N. Drain outlet
O. Oil-test bottle
P. CO_2 inlet
Q. Oil inlet
R. Vacuum inlet
S. Compound gauge 30 inches–0–30 pounds
T. Absolute manometer
U. Oil return *A*
V. Oil return *B*
W. Vacuum oil outlet
X. Joint drain

was done was in no small measure due to their lengthy experience and to the high quality of workmanship of their skilled staff.

Again under the engineer in charge was a staff of inspectors and engineers. The former checked each operation and dimension as performed. The latter were responsible for "treating" that is, evacuating and filling) all accessories, checking accessories as received, keeping reservoirs filled, and operation of the degasser. They were assisted by truck drivers who became skilled in handling the degasser and in many other duties. In addition, the utility maintained their own staff of inspectors checking and co-operating with the foregoing.

The installation of an oil-filled system involves planning for, training for, or executing the following:

1. Provision of suitable stock room, field office, tools, supplies and equipment, especially portable degassing equipment.

2. Provision for temporary source of degassed oil for feeding cable after pulling into ducts and before jointing.

3. Training of personnel, technical, semi-technical, and skilled tradesmen, in handling of cable and accessories, joint making and accessory installation.

4. Transport of cable and accessories from delivery point to point of installation.

5. Test check of cable or accessory for any leaks or imperfections before starting installation.

6. Actually installing, jointing, and so forth.

7. Continuous inspection and supervision of all operations.

8. Keeping of detail records of each individual operation, progress records, charts, and a daily log.

9. Making field check tests.

The basic considerations are that no air or other gas shall remain in, or in contact with, the cable oil, nor shall any overheated oil remain in the system. All training and inspection keep these prime necessities in view at all times. As detail procedure and instruction sheets are quite lengthy, this article can give only the general principles and procedure followed.

1. Tools and Equipment

(*a*). A portable degasser, a vital piece of equipment, (Figure 11) serves as a source of high vacuum (less than one-millimeter absolute pressure) and as a source of warm degassed oil. The same special oil used in impregnating the cable is taken to the field in 50-gallon sealed drums. Here it is tested for breakdown strength and color, drawn into the portable degasser, filtered, and sprayed under high vacuum into the main tank of the degasser. It is then recirculated, through a rigidly controlled heat exchanger, and again sprayed into the high vacuum until it reaches approximately 60 to 65 degrees centigrade and is thoroughly degassed. A viewing window in the dome of the degasser permits ready inspection of the oil condition. Vacuum and pressure

gauges, thermometers and thermostatic controls, automatic cutoffs, and similar safety devices make this degasser a highly reliable and efficient instrument. During the larger contract two degassers were employed, one mounted in a trailer, the second in a truck.

(*b*). A "treating bottle" and its associated valve panel (Figure 12) are employed at the joint or accessory being treated. These embody a system of valves and vacuum and pressure gauges by which the oil or vacuum from the degasser can be applied to the accessory being treated, the degree of treatment checked, and oil from the degasser checked for quality before being admitted to the cable system.

(*c*). Extra-heavy-walled one-half-inch (inside diameter) special rubber hose is used as vacuum line from the degasser to the work. Soft copper tubing three-eighths inch (outside diameter) is used as the oil line.

(*d*). A plentiful supply of three-eighths inch (outside diameter) soft copper tubing and flare fittings such as nuts, tees, crosses, unions, and so forth was maintained in the field stock room with the degasser and with the inspection crews.

Spare manometers, both atmospheric and absolute types, pressure-vacuum gauges, mercury, a small hand-operated high-grade vacuum pump, appropriate wrenches, spanners, and miscellaneous tools were found essential.

(*e*). Report forms were set up in great detail, individual forms covering the following:

Pulling.
Manhole and reservoir inspection.
Normal joints.
Stop joints.
Terminals.
Degasser log.
Truck log.
Progress charts.

2. Temporary Source of Degassed Oil

As the permanent reservoirs were available at the time cable started arriving at the site, these reservoirs were mounted at or close to their permanent locations. Cable deliveries were scheduled so that lengths adjacent to the reservoirs were first pulled in and connected to the reservoirs, the next lengths connected to the first by temporary oil lines, and so on. This eliminated the use of numerous temporary reservoirs and introduced many economies such as less frequent recharging, handling of reservoirs into and out of manholes, besides the investment cost in such temporary reservoirs. No troubles were encountered, nor was it felt that there was added risk with this technique as compared with practice common in United States.

3. Training of Personnel

Before jointing started, a school was instituted in which actual joints were made under simulated manhole conditions. Jointers and inspectors were required to do each operation of their respective duties in this school. All personnel handling the degasser were required to practice its operation until thoroughly conversant with it.

4. Test Checks

Each cable length at the site was checked by means of an "expulsion" test (Figure 13) to ensure that it was properly impregnated in the factory and that no gas had entered the cable thereafter.

Figure 13. Expulsion test on reel length

Each reservoir, pothead, feed pipe, and valve panel, before installing and after installing but before filling with oil, was checked for leaks by evacuating and watching carefully for loss of vacuum when blanked off from the source of vacuum.

5. Installation

(*a*) *Cable.* Cable pulling was done by standard methods with careful records of pulling tensions and atmospheric conditions (some of this was done at temperature in the order of 0 degrees Fahrenheit). As the pulling eye enters the manhole toward which the cable length was being pulled, it was connected through a fitting screwed into the pulling eye to a source of degassed oil. The other end of the cable was then disconnected from the reel oil reservoir and pulling completed.

Coefficients of friction computed from recorded pulling tensions are shown in Table A.

Table A. Coefficients of Pulling Friction

	At Average Temperatures of	
	Cold Weather Minus 7 C	Warm Weather 23 C
Maximum	0.520	0.370
Minimum	0.190	0.120
Average	0.312	0.258

(*b*) *Reservoirs.* After erection and with valves installed, reservoirs were checked again by blank off. In the case of the Canadian-built reservoir of new design, calibration was effected as follows:

The cells were completely collapsed and degassed by being placed under vacuum. Gauge oil was then poured in through the riser tank until at the normal zero on the gauge glass. The volume of oil contained by the cells, when at operating zero, (minus 0.1 pound per square inch) is five gallons for these 36-gallon reservoirs. Accordingly, five gallons plus or minus an amount to allow for the contraction or expansion of the gauge oil between ten degrees centigrade (assumed normal temperature) and its temperature at the time of charging were drained from the gauge oil. The reservoir thus calibrated was then charged with degassed oil to whatever volume was required for the system at the time.

(*c*) *Oil Lines.* The permanent oil lines were of copper pipe with joints of the soldered capillary type. Each oil line was heated to remove moisture, tested by blank off for vacuum tightness, flushed with clean oil at 110 degrees centigrade and, if not for immediate use, was backfilled with dry carbon dioxide gas. When about to be placed in service the pipe was flushed with degassed oil, reevacuated, filled with degassed oil, and connected to a source of degassed oil fed from a reservoir.

(*d*) *Joints and Potheads.* As these were points at which the cable insulation and newly applied insulation were necessarily exposed to the atmosphere, special means were employed to remove such contamination. This paper, because of lack of space, does not go into details of splicing and erection, but outlines the added requirements above ordinary solid-type cable joints consequent on the type of cable and the principles followed in ensuring that the quality of the cable was not impaired.

1. All oil overheated by soldering operations must be flushed out.
2. There must not be a stoppage of oil channels when work is completed.
3. No sharp stress points may be left.
4. Proper contours must be maintained.

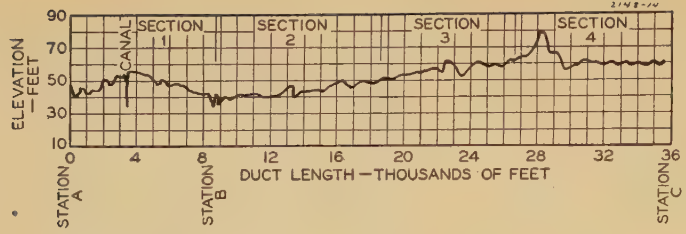

Figure 14. System profile of 120-kv duct line

5. There must be no leaks, even the most minute.

6. Dissolved and occluded gas must be removed.

7. The accessory must be left filled with good degassed oil.

These last two requirements are accomplished by the so-called "treatment." The joint or pothead was connected through the treating bottle to the portable degasser. Vacuum was applied until the accessory had reached a high degree of vacuum and so maintained until, on being blanked off, the completeness of the treating process was indicated by the lack of rise in absolute pressure. Drainage of oil coming out of the insulation during this operation was effected through a drain connection at the lowest point in the accessory. Such drainage, of course, must be into a vacuum.

The accessory was then filled with degassed oil from the portable degasser and connected to the system excepting in the instance of joints when this first filling was drained out, the bottom joint fitting permanently sealed, and the joint retreated and refilled with degassed oil. The top fittings of normal joints were then permanently sealed, as they have no source of oil other than from the cable itself.

In the 300,000-circular-mil section joints were treated immediately after the splicer finished. In the 650,000-circular mil section the splicers completed the three joints in each manhole. The three joints were then "gang" treated (that is, all treated at the same time). (See Figure 7.) This routine permits most effective use of all personnel and equipment and results in substantial time saving provided the skilled mechanics do first-class work. During this phase of operations the gangs were organized for continuous work from 8 a.m. Monday to 8 a.m. Saturday, using three eight-hour shifts.

On account of the small number of stop joints and of other limiting circumstances in the case of potheads this routine was not followed in these cases. Each such accessory was treated as soon as the splicers were finished.

7. Inspection and Supervision

The keeping of detailed and continuous inspection of all operations and constant backchecking was a highly important element in the success of the work. It was found that the skilled mechanics welcomed this inspection and co-operated thoroughly with the inspectors. Besides entering it in the proper report, all unusual results or circumstances were required to be reported directly to the engineer in charge.

8. Keeping of Records and Log

Detail reports of personnel doing the work, time in and out, time each subdivision of the operation was completed, values of pulling tension, vacua, weather conditions, and so forth were turned in by the inspectors or engineers at the completion of the operation. These reports were then typed by the office man, and from them and other data, the "daily log" and progress charts were entered. From these data it can be determined what man did any one operation and under what conditions. Progress charts and records served their usual function of assisting in seeing that no operation was neglected and in correlating progress on all parts of the work and on preparation of progress invoices.

9. Field Check Tests

As work progressed, expulsion, impregnation, and oil-flow tests were made frequently. The former two tests checked that no air or other gas had become dissolved or locked in the system. The last named ensured that there was no stoppage or constriction in the oil channels, such as from a wrongly positioned connector valve in a joint, or from solder working into an oil channel.

Operation

Contracts for the first and second sections of this work were placed in May 1940. As they were approaching completion in May 1941, the contract for the third and much larger section was placed. The whole system received voltage tests and was placed in initial service on December 14, 1941, two weeks ahead of the original schedule.

In order to transmit as much power as possible when available, that is, during the off-peak hours, it was decided to rate the cables as follows: Since the original calculations were based on a $62^1/_2$ per cent daily loss factor, it seemed satisfactory to allow a total daily watts loss from all sources equal to that established by using the standard formula for current-carrying capacity and to use as maximum current that established by using the factory measured resistance of the conductor as indicating the copper area rather than the circular-mil area. Since more power was available between midnight and 8 a.m., curves were made to show the maximum current that could be carried during these hours, depending on the current carried during the previous 16 hours, and to give a loss factor of $62^1/_2$ per cent with no current higher than the maximum allowable by calculation.

Records of ground temperature, idle duct temperatures, sheath temperatures in manholes, duct-mouth movements, and oil demands are being taken, and it is hoped that in the future these will provide some valuable information.

Sheath-bonding transformers of the Halperin and Miller design are being installed on circuit *C* which will increase the present load-carrying ability of the cables a considerable amount and at the same time allow for the saving and transmission of the load that is presently taken by the sheath losses. Because of the time element these were not in the main contracts, but provision was made for these transformers by including solder-seal sheath insulators in the joint casings.

The oil level in reservoirs is supervised by a simple alarm system operated by high- and low-level switches at each reservoir. The high or low oil level, as soon as it exists, is known by a station operator who is able to report the section in trouble to the proper persons.

A further precaution is being taken with reservoirs in the form of a dehydrator attached to the reservoir breather. This consists of a cylinder filled with activated alumina through which the air in contact

Table IV. Bill of Principal Materials

Exclusive of Spares

	Produced in Canada	Imported
Cable		
300,000 circular mils	53,400 feet	
650,000 circular mils	81,500 feet	
Number of lengths	201	
Joints		
Normal	124	56
Stop		6
Terminals	21	9
Reservoirs		
36 gallons	24	
17 gallons		12
9 gallons		2
Oil-level relays	24	8

A New Single-Pole Service Restorer

E. E TUGBY
ASSOCIATE AIEE

Synopsis: The widespread and general use of three-phase automatic service-restoring equipment on low-cost low-revenue distribution lines has created a field for a single-pole service-restoring device having continuously adjustable characteristics for trip current and time delay, and powered by a prestored source of energy. The device described in this paper utilizes a new and unique application for the prestoring of the operating energy and the restoration of the utilized operating energy after transient fault conditions.

A STUDY of the requirements for a source of operating energy for this new single-pole service-restoring device indicated a need for a source that would be independent of fault current and have available a dependable source of prestored energy when a fault occurs.

The successful operation of a very large number of service restorers powered by manually wound or motor-wound torsional springs logically led to the investigation of a similar source.

From this investigation was developed a new and unique application of previously tried and proved principals. In a paper presented by A. E. Brock at the Pacific Coast convention in August 1941, an improved service restorer using a motor of an entirely new type for rewinding the operating spring was described.

Paper 42-147, recommended by the AIEE committee on protective devices for presentation at the AIEE Pacific Coast convention, Vancouver, British Columbia, Canada, September 9–11, 1942. Manuscript submitted June 1, 1942; made available for printing July 3, 1942.

E. E. Tugby is sales engineer, Pacific Electric Manufacturing Corporation, San Francisco, Calif.

This Vibratorque motor is adapted to this new application, but, instead of the customary secondary source of energy being provided, the motor is arranged to be directly in series with the primary line, thus making the new unit a completely self-contained device.

The operating energy of the service restorer is contained in a motor-rewound flat spiral spring where enough energy is stored to provide four opening operations and three reclosing operations on a sustained fault without rewinding, and an infinite number of operations on transient faults, as the Vibratorque motor restores the expended energy upon reclosing the circuit after each transient fault.

Figure 3 shows a schematic diagram of the entire mechanism.

Rating

This single-pole service restorer is rated at 15 kv 50 amperes continuous current-carrying capacity, with a maximum interrupting rating of 1,000 amperes when using the 25–50-ampere trip coils. When the lower current-rating trip coils are used, the interrupting rating is reduced to 40 times the rating of the trip coils installed.

The interrupting ability of this new service restorer has been amply proven by a comprehensive test program where currents of 1,490 amperes at 15 kv were interrupted on a full cycle of three reclosures and four openings.

A complete oscillographic record was made of the tests for record and study.

Two thousand operations were performed at moderate short-circuit currents considerably in excess of continuous current rating, on a 15-kv system, and, on being inspected, the service restorer was found in condition to perform many more operations.

A complete series of impulse tests was performed, and the results recorded on a cathode-ray oscillograph, to verify operation under lightning conditions, and in every case the restorer flashed over from bushing to ground outside the case.

Arc-Extinguishing Devices

The successful and co-ordinated performance of rural lightly loaded low-cost distribution lines under fault conditions depends primarily upon the rapid extinguishment of the arc and the high speed of the interrupting device protecting the circuit.

As the prestored spring operating energy is restrained by a roller-type latch,

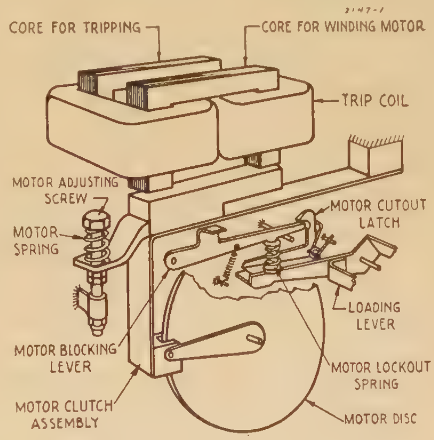

Figure 1. Diagram of Vibratorque motor

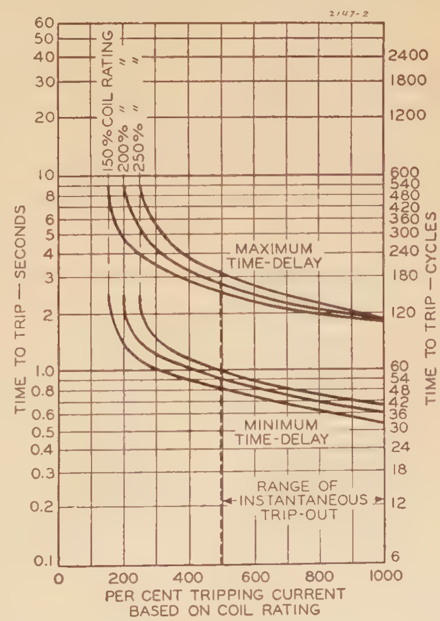

Figure 2. Time-current characteristic curves

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with the gauge oil (that is, the oil outside the reservoir cells) must pass before entering the reservoir. It is felt that by removing the moisture in the air, less sludging will take place around the reservoir cells. The size of these cylinders is such that, with expected load cycles, the alumina should not have to be reactivated more often than once a year.

Load has been carried continuously since January 2, 1942. Operating data are being collected, such as duct temperatures, oil demands, cable movement at duct mouths, shifting of joints, and so forth. Sufficient information of this nature to draw conclusions will require considerable time to collect.

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4. Specifications for Impregnated-Paper-Insulated Lead-Covered Cable, "Oil-Filled" Type (third edition). Association of Edison Illuminating Companies, February 1938.

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Table I. Typical Interrupting-Capacity Tests

Test Item	Number of Tests	Amperes	Average Arcing Time (Cycles)	Duty
1	10	104	1.0	O—5 sec—*OCO—5 sec—OCO—9 sec—OCO
2	6	640	1.1	O—7 sec—OCO—7 sec—OCO—7 sec—OCO
3	6	1,459	0.99	O—7 sec—OCO—7 sec—OCO—7 sec—OCO

* OCO—Open, close, open.

and the fault current affects only the latching mechanism, the operating speed of the arc-interrupting device is independent of fault current. The moving blade travels with a high rate of speed to assure quick extinguishing of the arc in the expulsion chamber, and an actual test performed has shown arc time from 0.65 to 1.2 cycles of arc while interrupting approximately 1,500 amperes at 12.5 kv to ground. The blade tips of arc-resisting Elkonite also assure a minimum of material vaporization, thereby minimizing oil carbonization and consequently reducing maintenance and inspection.

A test run of in excess of 2,000 operations at a moderate short-circuit current and full voltage failed to reveal serious burning of contacts which gave complete proof of the adequacy of the design.

Rewinding Motor

The rewinding motor is of a unique design and has a number of novel features not available heretofore.

A cross section of this Vibratorque motor (shown in Figure 1) illustrates the principle involved.

In referring to this drawing, it will be seen that an armature is mounted on a spring that is rigidly supported at one end and has an adjustment to limit the travel of the spring at the other end. At right angles to the armature is an extension carrying a clutch that engages both faces of a flat hard steel disc. The clutch consists of two hardened steel-bearing rollers engaging in a tapered slot, so co-ordinated as to provide the most efficient transfer of energy from the vibrator to the steel disc, with the least friction on the return stroke. The armature is alternately attracted by an a-c magnet coil assembly consisting of two identical coils mounted on a laminated solenoid core, and returned by a spring. The reciprocal motion of the vibrator alternately causes the clutch to engage on the disc which travels forward a small increment, then to disengage and move back to its original position. The disc rotates in a rapid series of small increments which are transmitted through gears to the main spiral operating spring, where the energy is stored for use in operating the service restorer.

The speed of the output shaft is a function of the weight and size of the armature, ampere turns of the coil, and characteristics of the spring which supports the armature.

The Vibratorque motor develops compound characteristics and with a change in load assumes a moderate output speed change. The magnet coils which also act as trip coils are, as previously stated, connected in series with the line and arranged in two halves to give flexibility. Since they are in series with the line, no external source of operating energy is necessary, and, as the rewinding energy is provided during normal operating conditions, the stored energy is instantly available to operate the device during fault conditions.

The motor will operate successfully with the smallest coils when a current as low as one-half ampere flows through the line.

The magnet coils also perform a dual duty as trip coils and are available in ratings from 3–6 amperes to 25–50 amperes. It is to be noted that pairs of coils have a dual value; as, when the coils are connected in series, they have their lowest rating, that is, 3–6 ampere coils would carry three amperes, but if they were connected in parallel, they would have their higher rating of six amperes as a unit, with each coil carrying three amperes only.

When the Vibratorque motor has fully wound the operating spring to its required tension, the vibrator spring is automatically locked up against the pole pieces. This is controlled through a differential arrangement on the outer case surrounding the operating spring. A scroll is cut in this outer case, in which rides a stud projecting from a bar which thus travels across the face of the case and assumes a position with direct relation to the amount of spring operating energy available. With the spring fully wound, the projection is in a position near the outer periphery of the spring house, and the bar projects beyond the case. When this point is reached, a catch that is normally held under tension is released, and sufficient pressure is applied to the armature support spring to force it up against magnet coils and prevent further vibration. When the spring energy is expended, the differential bar moves down, under the action of the projection in scroll and re-engages the catch, which

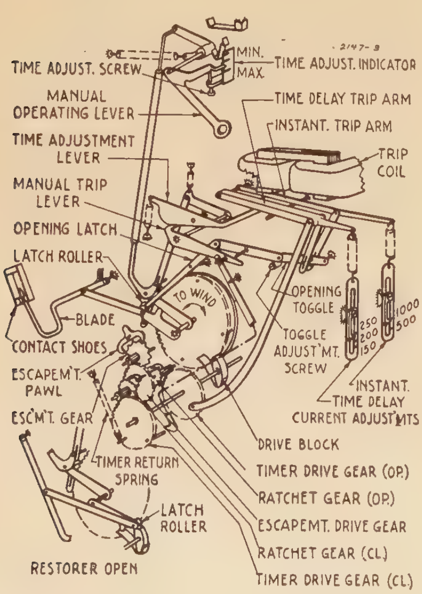

Figure 3. Diagram of mechanism

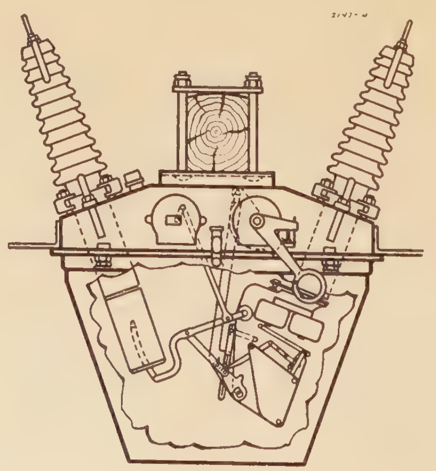

Figure 4. Cutaway section of complete unit

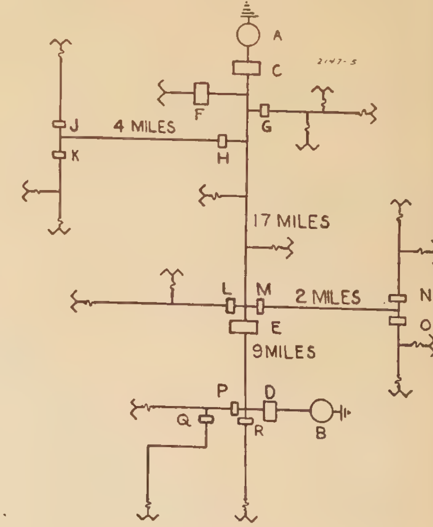

Figure 5. Typical sectionalized system

releases the spring to vibrate and again supply power to the operating spring.

The center of the coil spring is connected to a shaft which carries on its outer end an operating lever, which in turn is connected to the moving contact blade through a connecting rod.

This blade is normally held in the closed position by the operating spring, restrained by a roller latch mechanism. This type of latch mechanism has been found to give positive latching while requiring a minimum of current for tripping without any diminution of the tripping speed.

The latch may be released by one of the three following methods:

1. By the trip coils actuating the time-delay mechanism.

2. By the trip coils actuating the instantaneous high-current trip mechanism.

3. By the operation of the manual trip mechanism.

All three of these schemes act upon a single common toggle independently and, in the case of the first two, are independently adjustable over a wide range.

Escapement-Type Delay Mechanism

As the complete service restorer was designed for low-cost protection, it was imperative that all working parts be utilized to the fullest degree. Since both tripping and reclosing involved time delay, it was decided that one unit serving both operations could perform these duties admirably, and a mechanism was accordingly built with a rugged design that would handle both duties successfully and reliably.

This time-delay mechanism consists essentially of a gear train with an escapement pawl, which provides a time delay that is independent of temperature conditions.

The time-delay driving gear has a ratchet on either side, and each ratchet is connected to a smaller gear on a shaft. On one side this ratchet gear is driven by the opening gear which is connected to the opening toggle, and the other ratchet gear is actuated by the main blade through a suitable mechanism to control the reclosing time delay.

The correct initial starting position is always assured by a cam return spring that brings the gear train back to normal starting position. If the fault clears before the restorer opens, the time delay will immediately return to its initial position ready for a second fault, thus providing a high degree of accuracy of protection. The reclosing time delay is actuated by a projection on the main-blade operating lever. This projection initially engages the flat spring mounted on a cam which initiates the movement of the gear train. After a short travel, the main-blade operating lever engages the cam, and the driving effort of the main operating spring, transmitted through the main-blade operating lever, tends to rotate the cam, but rotation is restrained by the time-delay escapement.

The reclosing time schedule is fixed at seven seconds, but the tripping time delay may be varied. The tripping time is dependent upon the amount of travel of the escapement, which in turn may be limited by a time-adjustment lever. This lever adjusts the position of the trip arms relative to the trip-coil pole faces and is in its turn controlled through a linkage to the manual rewinding lever. This lever is adjustable to its final position by a set screw mounted external to the housing. Calibration points are provided to indicate the maximum and minimum adjustments, which may be easily adjusted in the field with a screw driver.

Manual Rewinding Mechanism

A manual rewinding mechanism is provided and so arranged that the restorer will not reclose until sufficient spring energy is stored for one closing and one opening operation, thus providing a valuable safety factor when closing in against a fault.

If there is no fault on the line, the rewinding motor then takes up its duties and completes the rewinding of the spring to its proper operating tension.

Time-Current Characteristics

Typical time-current characteristics are shown on Figure 2 where it may be noted that minimum trip is arranged for 150 per cent of rated coil current. That is, in the dual ratings with the coils of a 3–6-ampere combination connected in series for a three-ampere maximum current, the restorer would trip at 4.5 amperes, or if the trip coils were connected in parallel for six amperes continuous current, then the restorer would trip at nine amperes. The two sets of curves for minimum and maximum time delay represent the limits of the lever setting previously described, and each family of curves represents the current adjustment obtainable.

It should also be noted that at 500 per cent of rated coil current, an instantaneous trip mechanism comes into play, and the restorer may be tripped instantaneously. This minimum trip may be easily adjusted from 500 per cent to 1,000 per cent if a higher instantaneous trip value is desired. The current adjustments are both internal to the restorer and may be made with a screw driver as calibration marks are provided, and an indicator may be set for each adjustment.

Mounting

This new restorer is designed for either single-arm or direct-pole mounting and may be readily arranged for either type mounting as required.

Construction

Referring to Figure 4, it will be seen that this new single-pole service restorer consists essentially of a pivoted blade actuated by a small coil spring, mounted on the lower end of the one bushing and an expulsion contact mounted on the lower end of the other bushing. Both bushings are mounted, diverging outwardly in the cover, and are of the standard stud type. A slow-speed motor rewinds the operating spring after each reclosure, and this makes available an infinite number of reclosures on transient faults. On a sustained fault, the prestored spring energy is dissipated at such a rate that, after the fourth spring operation following three reclosures, the restorer will not reclose and is "locked out." To reclose the restorer after lockout, it is necessary to manually rewind with the manual rewinding handle for four or five strokes, to store enough energy for one closing and one opening operation before the restorer closes and the motor begins its rewinding cycle.

Application

The problem of providing high-quality protection against faults, and the maintenance of a reasonable continuity of service at a cost comparable with the revenue obtainable with long lightly loaded low-revenue lines have been brought to the fore more rapidly and forcefully with the shift of population from the cities to the country.

The availability of modern low-cost electric appliances in these rural areas has further heightened the pressure to maintain high-quality service, and, inasmuch as the average rural circuit is of the radial feeder type, it is of utmost importance that faults be localized as much as possible and confined to the least number of customers.

Due to the nature of the territory in which these rural lines are located and the types of low-cost construction used, the prevalence of faults due to lightning disturbances and to trees swinging lines together in wind storms and whipping under sleet conditions is very high.

Studies made in many parts of the country show that an average of 80 per cent of faults may be cleared, and service restored on a first reclosure, which thus justifies the use of reclosing devices on a rural system.

The development of the three-pole service restorer described at the AIEE 1941 Pacific Coast convention was one answer to the problem of high-quality protection for the main feeder, and the development of the single-pole unit described in this paper complements the three-pole unit to relieve the three-phase service restorers of a large number of operations, and to localize a sustained fault to any one particular feeder.

An interesting application is illustrated in Figure 5 based on a co-operative system with widely scattered individual customers and on a moderately heavy load. *A* and *B* are two interconnected generating stations with three-pole motor-rewound service restorers at *C*, *D*, and *E* in the three-phase interconnecting power lines, and at *F* to protect a large load. At restorer *E* potential and current transformers were installed in the main line, and power directional relays were provided to prevent generating station *B* from carrying the entire load if generating station *A* were out of service. Single-pole restorer *G* protects one branch line that is in a reasonably clear area and would not be subject to serious faults. Single-pole restorers *J* and *K* protect long lines radiating through heavily wooded area and are in cascade with a single-pole restorer at *H*. Similarly, single-pole restorer *M* also is in cascade with restorers *N* and *O* to protect another area in trees. Restorers *P*, *Q*, and *R* protect feeders in the vicinity of generating station *B*. As no fuses are to be used for sectionalizing protection on this system, the problem of co-ordination was simply one of superimposing the time-current characteristic curves of single-pole service restorers on those of the three-pole service restorer and arranging tripping time and current settings so as to provide a high degree of selectivity.

At each distribution transformer installation, individual fuse protection was provided; therefore, the problem resolved itself into a group of individual circuit calculations to balance all factors to obtain the greatest flexibility, at the same time restricting outages to the smallest area.

A check was made of the connected capacity on each branch feeder, and it was found that, in any individual leg to be protected, the connected transformer capacity did not exceed 20 kva. On the basis of a load factor of 50 per cent the load current would not exceed 1.5 amperes per circuit, which was well under the continuous current-carrying capacity of the smallest trip coil available.

Therefore all single-pole service restorers used were to be equipped with the same size trip coil, namely 3–6 amperes, and were arranged where there were two in cascade so that the unit nearest the source had the trip coils connected in multiple, for six amperes continuous current-carrying capacity, and those furthest out were arranged with the coils in series for three amperes continuous current-carrying capacity. It was then necessary to make field adjustments as to the time delay and instantaneous trip at various points to properly co-ordinate the entire system.

The three-pole service restorers *C* and *D* at the generating station were each equipped with a first instantaneous tripping action, followed by time-delay tripping on subsequent tripping operations.

It is thus to be seen that each branch line is fully protected by a single-pole restorer which will trip after a time delay. The main-line protective restorer at the generating stations, being equipped with instantaneous initial trip and instantaneous first reclosure, will operate to protect the entire system against heavy transient faults in the main feeders.

The single-pole restorers would be arranged so that the tripping time delay is less than that of the main-line restorer on the second trip out; thus under sustained fault conditions the single-pole restorer will take over the duty of isolating the fault, and the remainder of the system continues with uninterrupted service. Under transient fault conditions the single-phase restorers will clear a fault on the branch lines, of lesser magnitude than that required to operate the main-line restorer at either *C* or *D* and will automatically restore the expended operating energy upon the line circuit being re-energized.

In the case of the system under discussion, after three transient faults had been cleared, each subsequent operation of the single-pole service restorer may be considered as saving the expense of a service man with his transportation to and from the maintenance base to patrol the line and restore service. This saving of labor and transportation offers a considerable reduction in maintenance expense and at the same time provides a very high quality of service in rural areas, comparable to that obtainable in suburban areas, at a very low initial cost.

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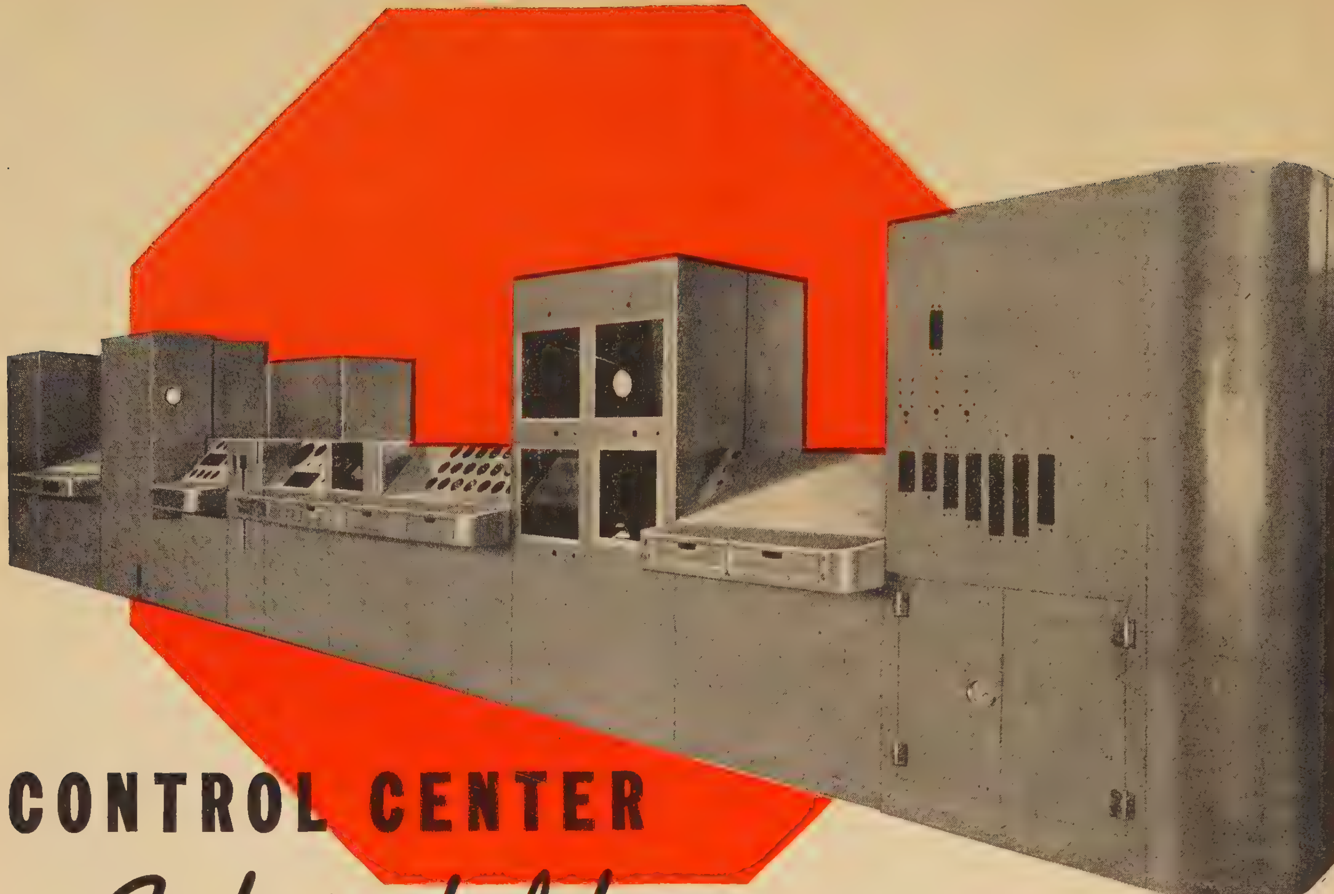

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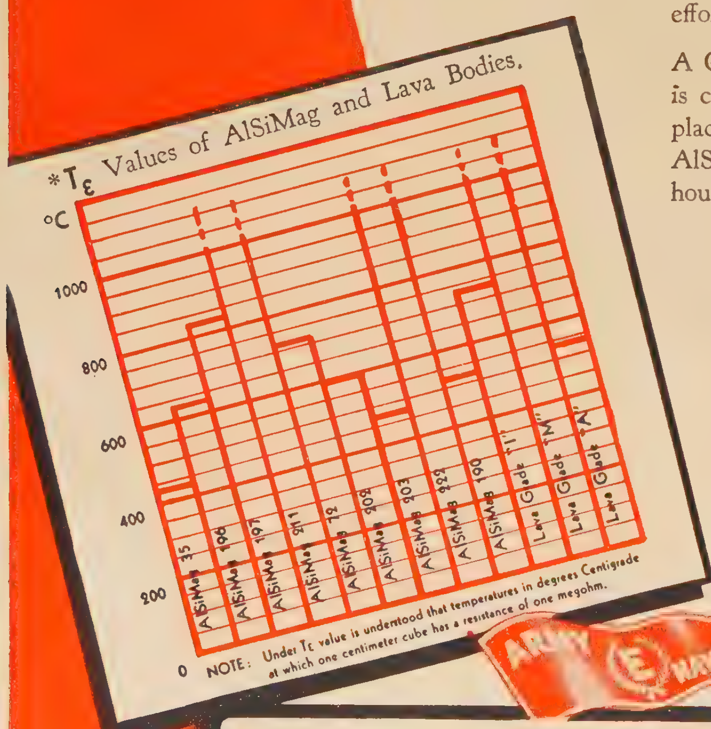
A Copy of this chart will be sent free on your request. It is conveniently arranged for filing, hanging on the wall or placing under desk glass. It takes only a moment to request AlSiMag Property Chart No. 416. It might save you many hours or days of laboratory experiment.



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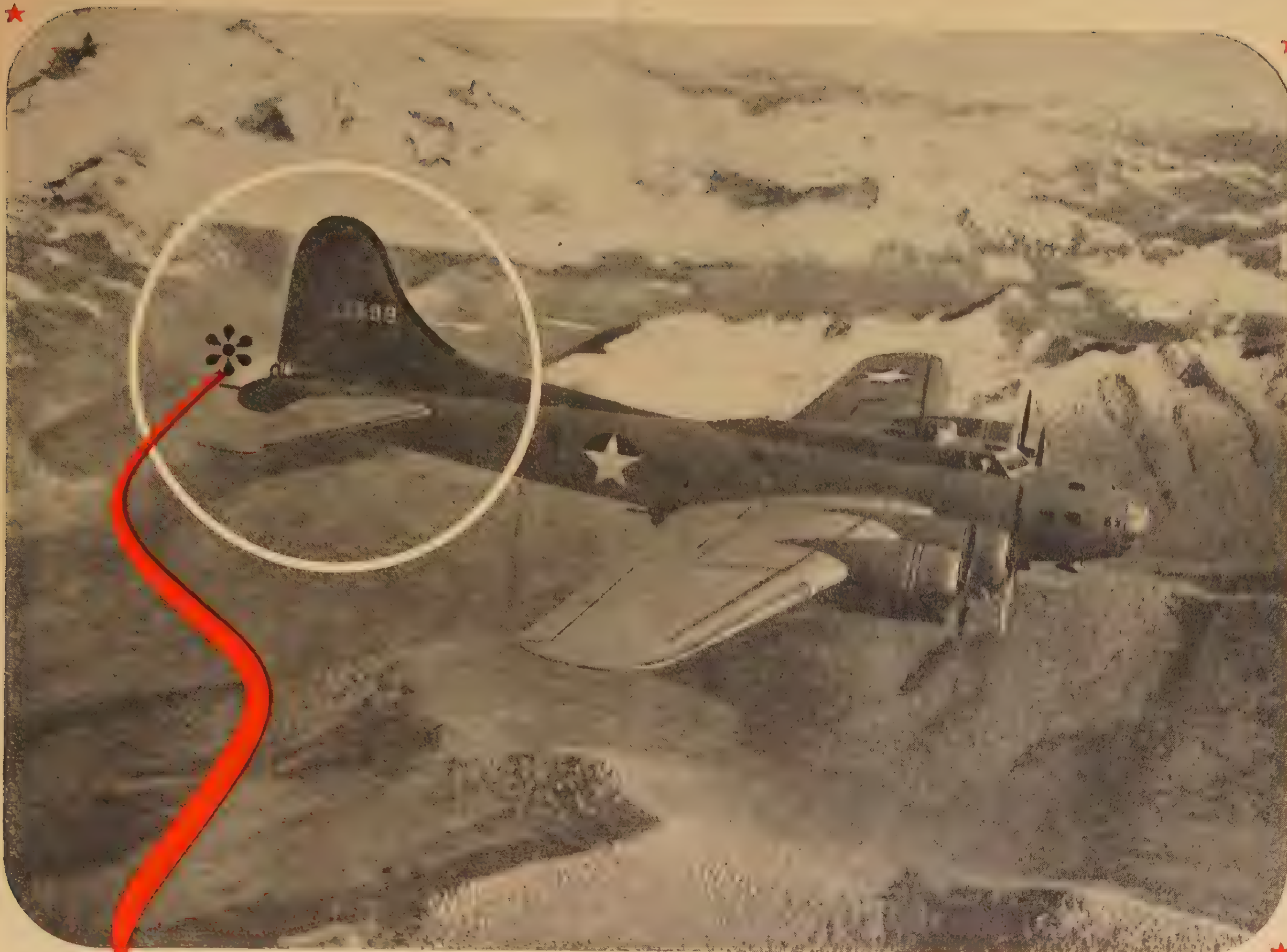

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VINYLITE Elastic Plastics are immune to many of the factors that cause rubber to deteriorate. They will not oxidize, and show no appreciable change even after prolonged aging. In abrasion resistance tests, certain types have proved superior to high-grade rubber compounds. Their tensile strength is higher than that of most rubber compounds. Under extreme climatic conditions, they do not become sticky or brittle — certain types retaining their flexibility even at −50 deg. F. Other types are non-flammable. Depending upon the formulation, unusual resistance to water, oils, and many corrosive chemicals can be obtained. Transparency is another unique feature impossible to obtain with rubber.

VINYLITE Elastic Plastics are available for war production needs in sheeting form and as compounds for molding, extrusion, and calendering. Sheeting is supplied in a wide range of transparent and opaque colors, in rolls of various widths and thicknesses. Compounds can be either injection or compression molded, extruded into continuous rods and tubes or into wire and cable coverings, or calendered directly onto cloth. For those industries which have the necessary processing equipment, such as the rubber industry, powdered VINYLITE Resins are available which can then be pigmented, blended with plasticizers, and formulated into flexible materials.

RUBBER-PROCESSING FACILITIES USABLE

Rubber manufacturers can realize important savings with VINYLITE Elastic Plastics. The handling of these materials is essentially the same as with rubber, and, with slight modifications, the same equipment can be used. However, no curing or vulcanizing is required, because VINYLITE Elastic Plastics come from the mold, extruding die, or calender mill as completed products.

If you are engaged in war production and seek flexible materials with the unusual resistance and durability of VINYLITE Elastic Plastics, we suggest that you enlist the full co-operation of our technical staff. Booklet P-5, "Vinylite Resins—Their Forms, Properties and Uses," describes these and other VINYLITE Plastics in detail.

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In recent tests, a special VINYLITE Elastic Plastic was found to be several times more resistant to abrasion than high-grade rubber compound.

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Insulation made of non-flammable types of VINYLITE Elastic Plastics is used widely on wire and cable in place of rubber and other dielectric materials.

NON-OXIDIZING AND NON-AGING

Oxygen bomb tests prove VINYLITE Elastic Plastics immune to oxidation. Thirty days' exposure to sunlight and weathering on coated cloth for Army raincoats also has no appreciable effect.

WATER AND CHEMICAL RESISTANCE

After 24 hours' immersion in water at 25 deg. C., absorption is only 0.20 per cent with certain types. Excellent resistance to oils, grease, and many corrosive chemicals can also be obtained.

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VINYLITE Resins can be pigmented to meet any color requirements. Unlike rubber, they can be formulated into transparent as well as opaque forms.

FLEXIBILITY AND STRETCHABILITY

Certain types withstand 3,000,000 cycles in a mechanical flexing tester. They may be elongated 300 per cent at room temperature, but will return to original length without "snapback."

ELECTRICAL INSULATION

Dielectric strength, in 0.025-in. thickness at 25 deg. C., ranges from 900 to 1,100 volts per mil, and 300 to 330 volts per mil in 0.125-in. thickness.

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Unlike rubber, complete products can be formed from VINYLITE Elastic Plastics by high-speed injection molding. This process brings marked economies on long runs.

COMPRESSION MOLDING

In many instances, VINYLITE Elastic Plastics can be compression molded in existing rubber molds, with minor modifications. Parts come from the mold ready for use — curing and vulcanization are avoided.

SCREW EXTRUSION

VINYLITE Elastic Plastics can be fabricated in standard types of screw extruders into continuous tubes, rods, and insulation coverings for wires and cables.

SPREADER COATINGS

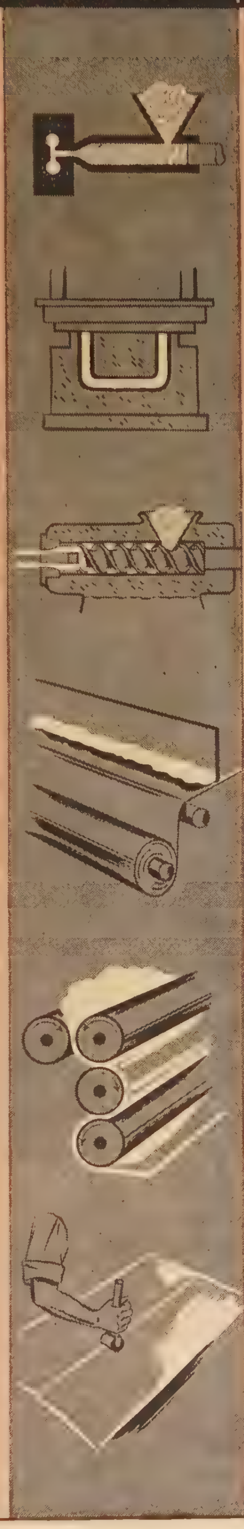
Dissolved in suitable solvents, VINYLITE Elastic Plastics can be applied to cloth on standard spreader-coating equipment.

CALENDERING

Durable, waterproof coatings on cloth, up to .030 inches thick, can be applied by standard calender mills used for rubber.

FABRICATING

Flexible sheeting and coated cloth can be either cemented or heat-sealed. Embossing is used to obtain various surface effects.



make possible many advanced wartime products...

INFLATABLE EQUIPMENT

Inflatable equipment, waterproofed by coating one or both sides of the cloth with VINYLITE Elastic Plastics is more durable than similar equipment made from rubber-coated cloth.

AIRCRAFT PARTS

Flexible even at —50 deg. F., VINYLITE Elastic Plastics are used for transparent aircraft wiring conduits, weather-resistant windshield wiper blades, floor mats, gromets, and wire terminal insulators.

RAINCOATS AND PAULINS

Army raincoats, tenting, and equipment paulins coated with VINYLITE Elastic Plastics retain their water resistance — without becoming brittle when cold, or sticky when hot.

UPHOLSTERY FOR HARD SERVICE

VINYLITE Elastic Plastics are used for upholstering seats in tanks and planes where they are far superior to rubber and artificial leathers in moisture resistance, toughness, and flexing life.

ELASTIC WIRE AND CABLE INSULATION

Durable insulation for vital electric wires and cables is made from VINYLITE Elastic Plastics. Non-flammable and abrasion-resistant, they eliminate additional protective wrappings.

CHEMICALPROOF PRODUCTS

Highly resistant to chemical fumes and liquids, VINYLITE Elastic Plastics provide flexible frames for gas masks, coatings for electroplating equipment, and durable tops for storage batteries.

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PLASTICS

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Bibliography on Electrical Safety

The third bibliography of technical literature entitled "Bibliography on Electrical Safety, 1930–1941" has just been published by the American Institute of Electrical Engineers. This publication, sponsored by the AIEE committee on safety, is designed to make available a fund of information on electrical safety which should be of special interest at a time when accident prevention is of national importance. A list of applicable standards, specifications, and safety codes is also included. Information on safety published before 1930 may be located through bibliographies accompanying articles listed.

The items in this bibliography are divided into sections according to subject matter as follows:

A. Electrical Accidents and Their Causes

B. Accident Prevention Methods

C. Safety Codes and Standards

D. Effects of Electric Shock

E. Resuscitation

The "Bibliography on Electrical Safety, 1930–1941" is a 16-page pamphlet, 8½ by 11 inches. It may be obtained from AIEE headquarters, 33 West 39th Street, New York, N. Y., at 25 cents per copy to Institute members (50 cents to nonmembers) with a discount of 20 per cent for quantities of 10 or more mailed at one time to one address. Remittances, payable in New York exchange, should accompany orders.

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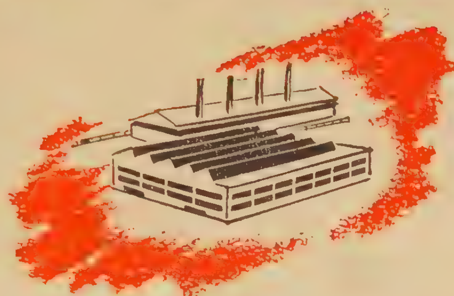
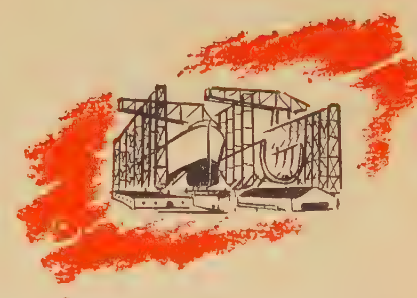


In a manufacturing plant, 8 tons



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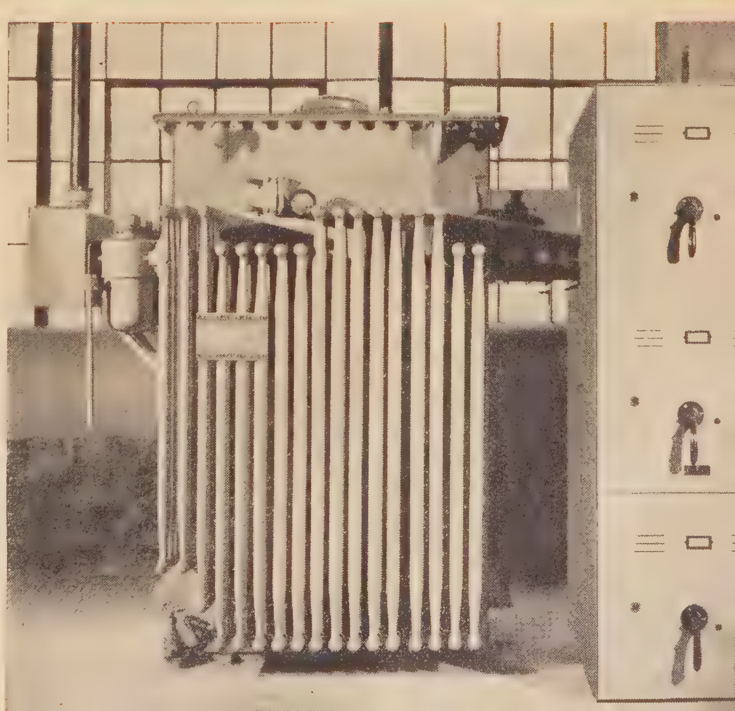

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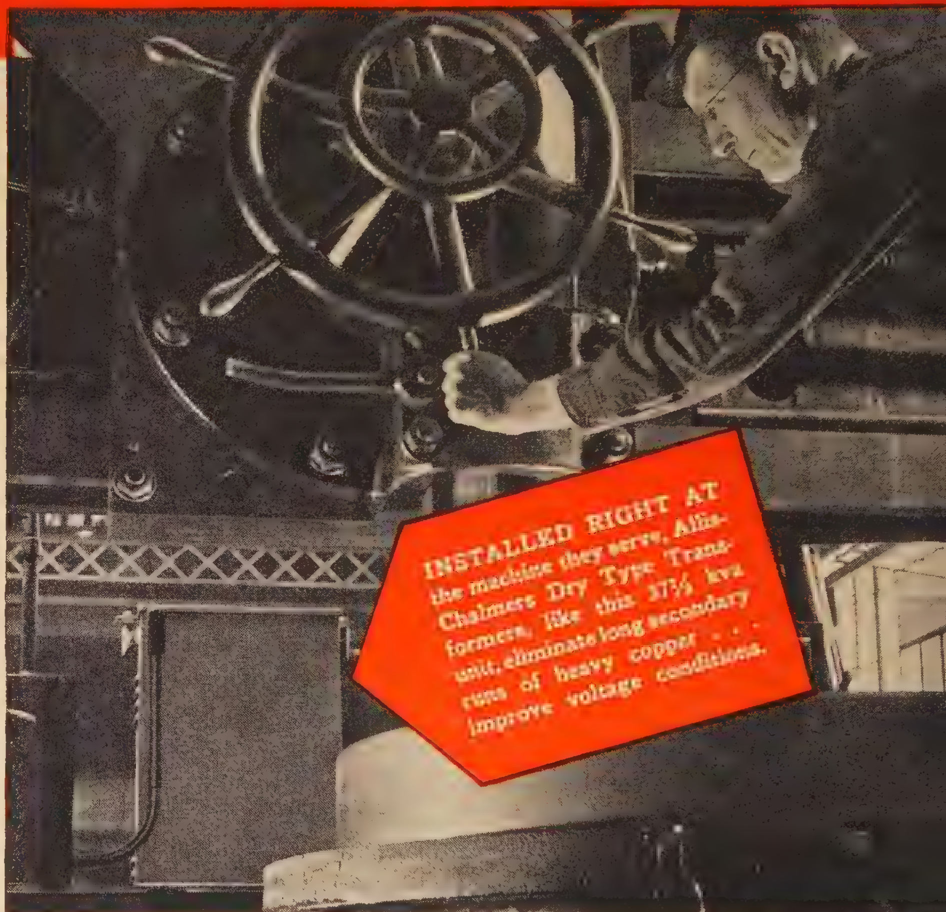

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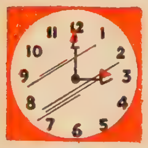

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TRADE LITERATURE

[Mailed to readers free—unless otherwise noted—upon request of companies named]

Stroboscopic Techniques.—Bulletin, 28 pp., "Eyes for Industry." Describes types of stroboscopes, typical uses, operating methods, etc. General Radio Co., Cambridge, Mass.

Signalling Devices.—Sectional Catalog. Describes industrial signals, bells, buzzers, chimes, horns, sirens, pushbuttons, relays, transformers. Schwarze Electric Co., Adrian, Mich.

Copper Conservation.—Bulletin 4210, 12 pp., "Saving Copper—A War Time Necessity." Delay in securing materials and shortage of man-power are forcing a revival of interest in open wiring up to 600 volts for industrial plants, according to this manufacturer. The booklet examines this type of construction from the standpoint of best use of copper. Delta-Star Electric Co., 2400 Block, Fulton St., Chicago, Ill.

Rheostats.—Bulletin 69. Describes pressed steel rheostats, applicable where the need exists for a small, sturdy, power rheostat having a large number of steps and ample current-carrying capacity. These rheostats are 4 inches in diameter with as many as 43 steps of control and are rated for 100 watts. They feature balanced contact arm, "dead" shaft construction, copper graphited contact shoes and front or back-of-board mounting in single and multiple assemblies. Ward Leonard Electric Co., Mt. Vernon, N. Y.

Industrial Rubber.—Bulletin, 30 pp., "Rubber Guide Book for American War Industries." Lists application and properties of many types of products for industrial and aeronautical purposes using natural, synthetic or reclaimed rubber. Sections are devoted to properties of Ameripol (synthetic rubber compounds), hydraulic equipment parts; sponge products; industrial rubber gloves; extruded rubber goods; anode process of rubber coverings, industrial and aeronautical molded goods; hard rubber parts; rubber cements; lathe cut rubber goods, and properties of reclaimed rubber. The B. F. Goodrich Co., Akron, Ohio.

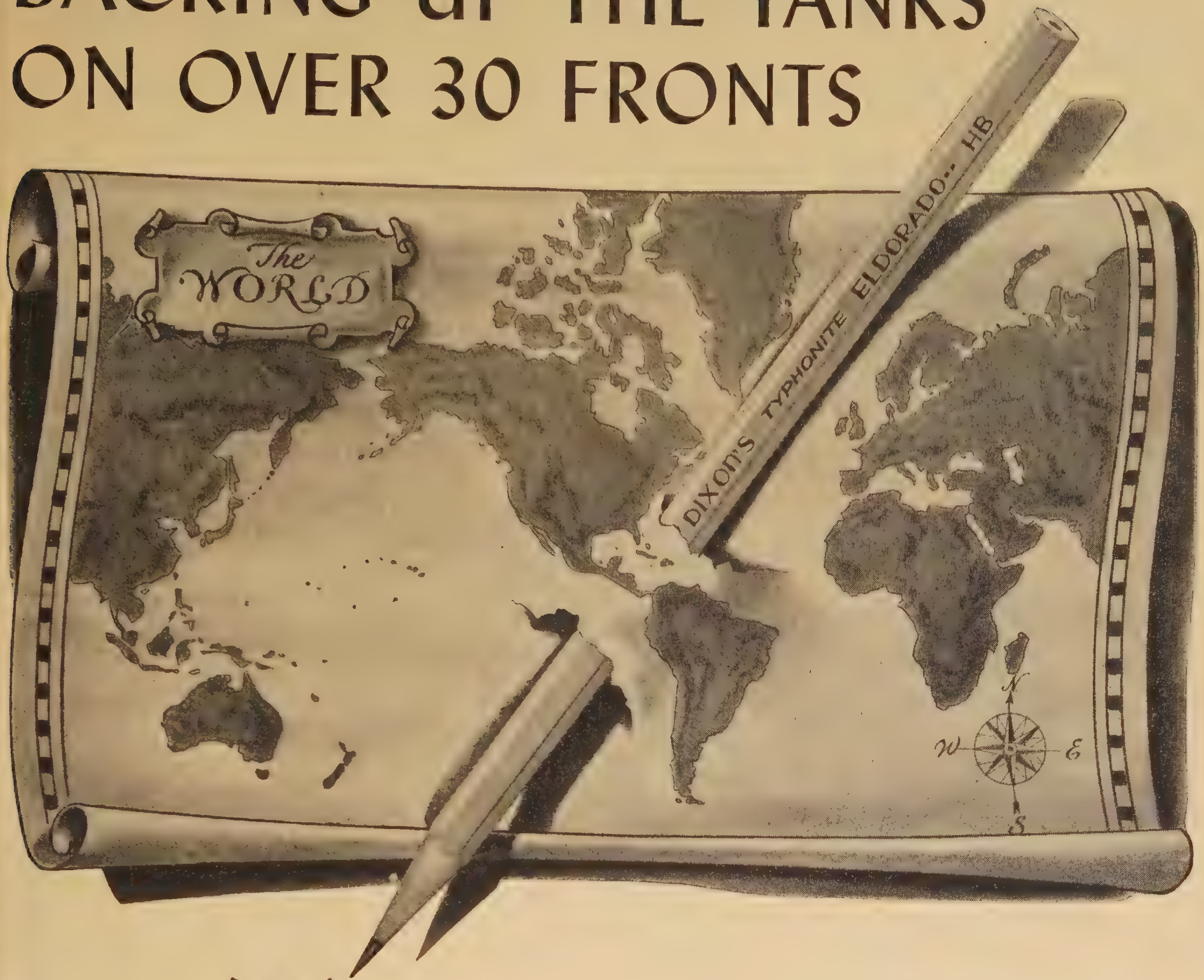
Industrial Plastic.—Booklet B-3184, 36 pp. Describes Micarta, an all-purpose industrial plastic—a replacement for many critical materials possessing extreme light weight; great tensile and compressive strength; high resistance to cold, heat, moisture, oil and mild chemicals; slow, even wear; excellent electrical insulation; easy to fabricate or mold. The Data Book discusses the manufacture of Micarta, and the two general forms in which it is supplied—laminated and molded. Comprehensive tables list standard Micarta shapes and the maximum and minimum dimensions in which they are available. Grade selection tables are included, and graphs indicate the relative mechanical, electrical and chemical properties of each grade. Westinghouse Electric & Mfg. Co., East Pittsburgh, Pa.

Metalclad Switchgear.—Catalog 1110, 12 pp. Describes horizontal drawout metalclad switchgear in 100,000, 150,000 and 250,000 kva capacities at 5,000 and 15,000 volts. The advantages of metalclad switchgear in safety, reliability and ease of installation and maintenance are emphasized. Construction details on design are described and illustrated; complete dimensional data on the various types of gear available are included. Roller-Smith Co., Bethlehem, Pa.

Silicon Bronzes.—"Duronze" Manual, 80 pp. Describes a series of high-copper silicon alloys which have found wide application in the electrical field, for transmission line hardware, nuts and bolts, connectors, wire and cable clamps, etc. Complete technical data and applications are included. Bridgeport Brass Co., Bridgeport, Conn.

Hydroelectric Units.—Bulletin B6184, 28 pp. Many of the large hydroelectric installations such as Boulder Dam, Santee Cooper, Pickwick Landing and Chicamauga, are described, as well as representative industrial and municipal units. Impulse, Francis and propeller type turbines are covered. The special adaptability of each type of turbine in relation to conditions of the installations is discussed. Allis-Chalmers Mfg. Co. Milwaukee, Wis.

BACKING UP THE YANKS ON OVER 30 FRONTS



The whole world is our battlefield in this war! To win we have to knock out the enemy wherever he is! You find Yanks in the Solomon Islands, China, Madagascar, India, Egypt, the Belgian Congo...and every other place that's a battlefield today or tomorrow! We've got to get there "firstest with the mostest." And come hell or high water, we shall not fail to deliver the goods.

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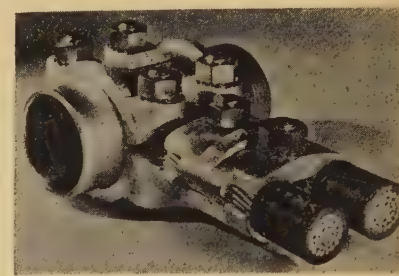
NEW PRODUCTS

Gem-Quality Synthetic Corundum.—Synthetic white sapphire, the mineral corundum unpigmented and of gem quality, is now available in the form of boules from the Linde Air Products Company, 30 East 42d St., New York, N. Y., a unit of Union Carbide & Carbon Corporation. The boules, the form in which the sapphire is manufactured, each weigh at least 150 carats and are of a regular cylindrical shape, enabling gem cutters to standardize on cutting and sawing procedures. The material, as now made, is practically perfect, any imperfections being microscopic in size and having no effect whatever on the quality of the material for precision bearing surfaces and other essential industrial parts. Many essential uses are already being made of jewels cut from these synthetic boules.

Among locations where they serve economically are as the jewel bearings of chronometers, compasses, and electrical, fire-control, and aircraft instruments. In such instruments, they are employed in the form of ring bearings or V-type and cup-type end bearings.

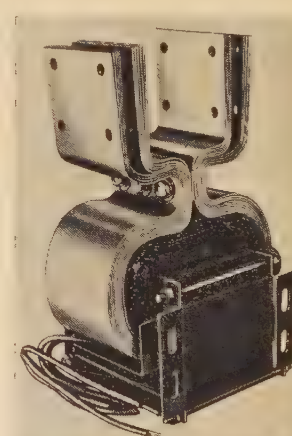
Varnished Silk Alternates.—Varnished rayon, varnished cotton cloth and varnished nylon have been developed by the Irvington Varnish & Insulator Co., Irvington, N. J., for electrical insulation formerly provided by varnished silk. All these materials possess good dielectric strength with tensile and tear strengths equal to or better than varnished silk and can be punched into special shapes. They are available in thicknesses from 0.003 inch to 0.008 inch in straight cut rolls or bias cut strips in 51 inch lengths. Each base material is coated with Irvington special insulating varnish. High tenacity varnished rayon is the most suitable alternate for varnished silk, comparing favorably with it in strength and flexibility. It has a dielectric strength of 1200 VPM and is used for wrapping leads, small magnetos and coils. Varnished cotton cloth has greater tensile strength than varnished silk. Its pliability permits application on odd shapes. Dielectric strength is 1200 VPM. Varnished nylon has qualities of flexibility and high tensile strength with dielectric strength of 1200 VPM. At this time, nylon is only available by Government allocation.

Connector.—This power connector, introduced by the O. Z. Electrical Mfg. Co., Brooklyn, N. Y., is a compact, flexible fitting made with two- or three-wire branch taps, and taking a wide range of wire sizes.



With its use, there is a saving not only in installation time, but in elimination of the additional connector equipment ordinarily required with a one-tap style connector. The interlocking clamp incorporated for positive gripping of wires, binds all strands tightly in the lug. The accurate machining of parts and the high copper alloy used in this type "KTC" connector assure full conductivity, corrosion resistance and long service life. Hex-head bolts are held fast by recesses in the ruggedly constructed body of the connector; make single wrench installation quick and easy.

Welding Transformer.—Speeding production of war matériel has created manufacturing problems oftentimes never experienced in the production of peacetime goods. As a result much production machinery has been designed to perform a specific operation or function. As an example, the Acme Electric & Mfg. Co., Cuba, N. Y., announces the development of special welding transformers for operation on primary circuits of 115 volts, single phase, 60 cycle and having secondary characteristics of 0.75 volt, 1600 amperes. (*Continued on page 22*)



GENERAL RADIO COMPANY

FOUNDED 1915

Manufacturers of RADIO AND ELECTRICAL LABORATORY APPARATUS

CABLE ADDRESS, GENRADCO, BOSTON
BENTLEY'S CODE
TELEPHONE TROWBRIDGE 4400

THIRTY STATE STREET
CAMBRIDGE, MASSACHUSETTS

TO USERS OF
GENERAL RADIO INSTRUMENTS

Gentlemen:

When instruments, built for intermittent service, are worked "around the clock" -- when years of normal instrument operation are compressed into a few months -- prompt repair service is essential. There must be no avoidable interruption of urgent war production.

Our service department is organized and equipped to handle today's unprecedented volume of service problems promptly and capably. Years of experience with adjustments and repairs have long since convinced us of the importance of service. Today this service is doubly necessary. A complete case history file is kept on every major instrument. Reconditioned instruments are guaranteed for one year to the same specifications as when new.

You can help to speed necessary repairs by doing all possible repair work in your own plant with the aid of our SERVICE AND MAINTENANCE NOTES. They have already helped hundreds of General Radio customers to cut down returns for repairs, to save valuable production time, and to get better service from their equipment.

When requesting your set of SERVICE AND MAINTENANCE NOTES, please list your General Radio instruments by type number and serial number.

Sincerely

H. H. Dawes

Service Manager

NEW PRODUCTS

(*Continued from p. 20*)

Blueprint Trimmer.—This simple trimming machine permits speedy and accurate cutting or trimming of drawing paper, blueprints, cardboard, etc. The material to be cut is automatically held in place by spring hinges placed on the rail guide. Cutting is performed back and forth by two old razor blades on a sliding block and secured in

place by nuts in a protected arrangement. The table is calibrated to provide margins around the frame from $^3/_8$ to 2 inches. The trimmer is made in four sizes, from 32 inches over-all with 25 inch cutting size, to 62 inches over-all with 55 inch cutting size. Manufactured by Edi Trimming Machines, 609 West 115 St., New York, N. Y.

Photocopy Machine.—A photocopy machine manufactured by the American Photocopy Equipment Co., 2849 N. Clark St., Chicago, produces copies similar to photostats, the actual size of the original, of anything written, typed, printed, drawn or photographed, at the rate of 120 copies per hour. No technical skill or dark room is required for operation. Engineering departments use it to make immediate copy directly from blueprints without making tracings, also for copying specifications, etc. Executive and sales departments employ it for copying important contracts and agreements, copies of inquiries, etc. The machine is moderately priced.

Among Recent AIEE Publications

Bibliography on Electrical Safety (*see page 10, adv. sec., this issue*)

•

Bibliography on Circuit-Interrupting Devices (*page 24*)

•

Advanced Methods of Mathematical Analysis as Applied to Electrical Engineering (*page 36*)

•

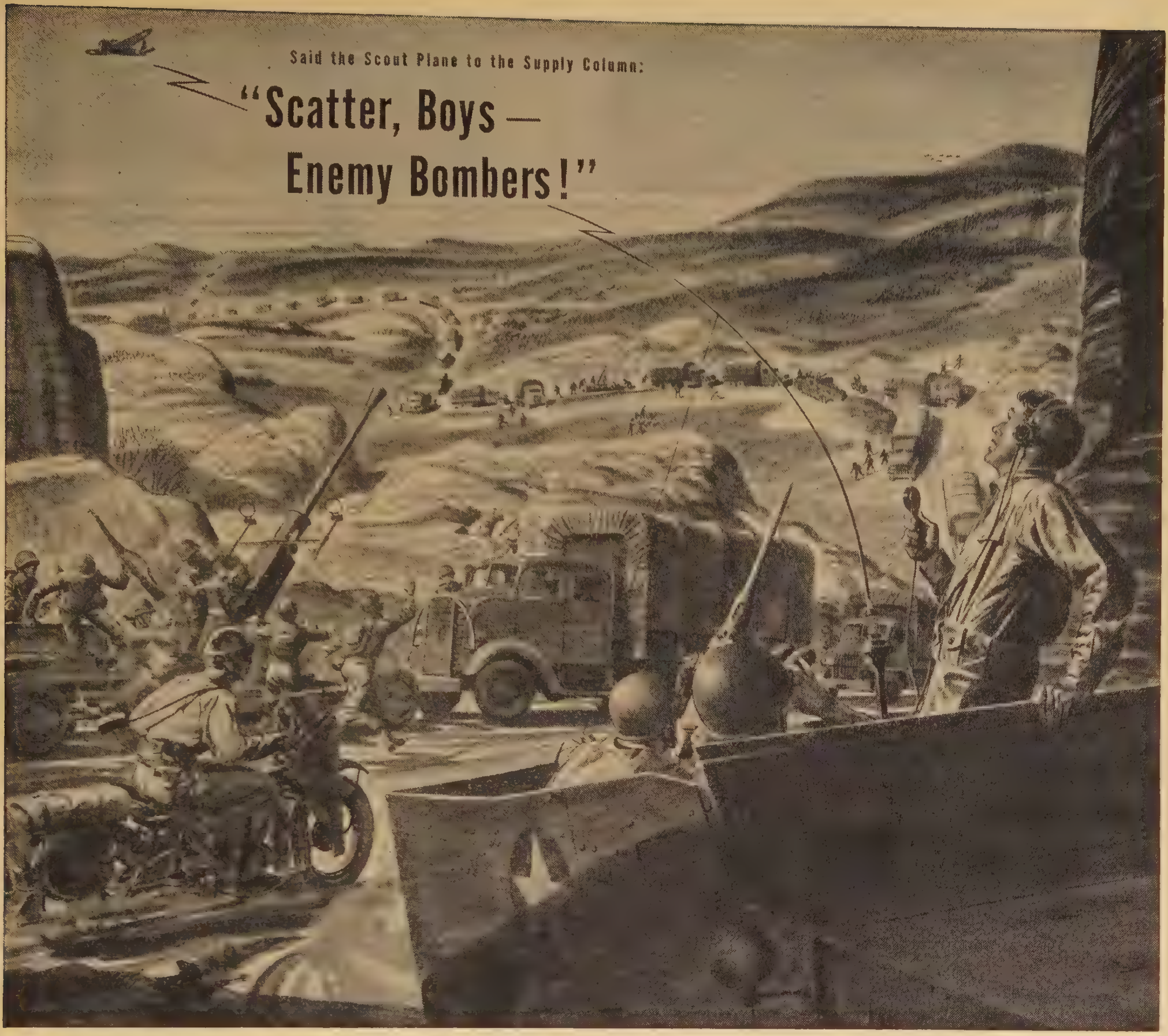
Two Resistance Welding Aids (*page 42*)

•

Electrical Definitions (*page 33*)

•

New AIEE Standards (*page 35*)



They work together better . . .
because they can talk together

Ten times faster
Than the crawling column they come

But faster still flashes the warning—
"Enemy bombers!"

Quickly trucks disperse
Guns are set up
And a warm welcome
Of good American ack ack
Makes the planes of the rising sun
Set.

What if this *teamwork*
Between scout plane and supply column
Were not possible?
What if there had been no
Radiotelephones?

This convoy
Might not have got through

And at its distant destination
Valiant allies
Might have gone down—fighting
Without arms.

Modern communications equipment
Designed and manufactured
By I. T. & T. associate companies
Is helping Uncle Sam
Coordinate his fighting forces
On land, sea and in the air.

The broad peacetime experience
of I. T. & T.
In the field of communications
Is proving its value in time of war.

INTERNATIONAL TELEPHONE AND TELEGRAPH CORPORATION *67 Broad St., New York, N. Y.*

I T&T *Manufacturing Associate:* FEDERAL TELEPHONE AND RADIO CORPORATION
formerly operating under the names: International Telephone & Radio Manufacturing Corporation *and* Federal Telegraph Company

Ferranti

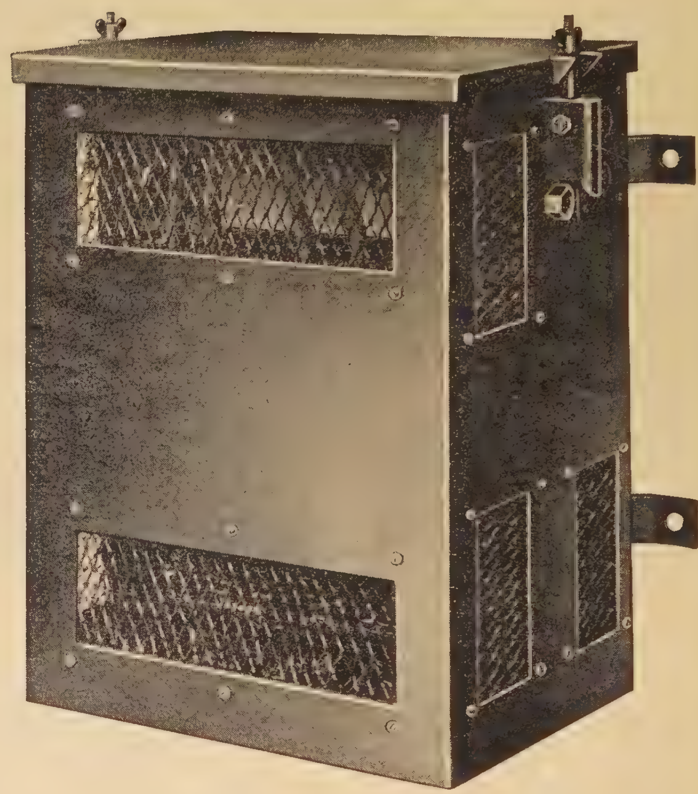
AIR COOLED INDUSTRIAL TRANSFORMERS

It doesn't matter what your problem may be—we can solve it for you!

The field isn't new to us—we've been in it for over sixty years!

To see YOU through the present emergency we have doubled and redoubled our manufacturing facilities—and we can give you surprisingly quick deliveries.

In the interest of NATIONAL ECONOMY you can best do YOUR share by cutting costs with Ferranti AIR COOLED TRANSFORMERS!



Also special transformers for every electronic, communications, sound and laboratory use—aircraft and portable work.

Send us your specifications—we'll do the rest!

FERRANTI ELECTRIC, INC. 30 Rockefeller Plaza New York, N. Y.

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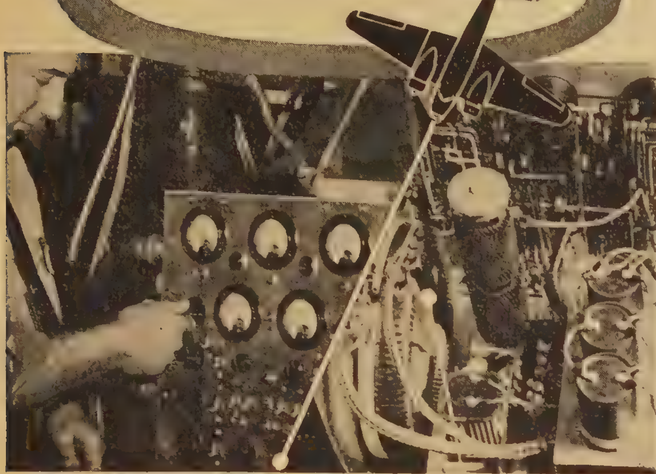
OHMITE

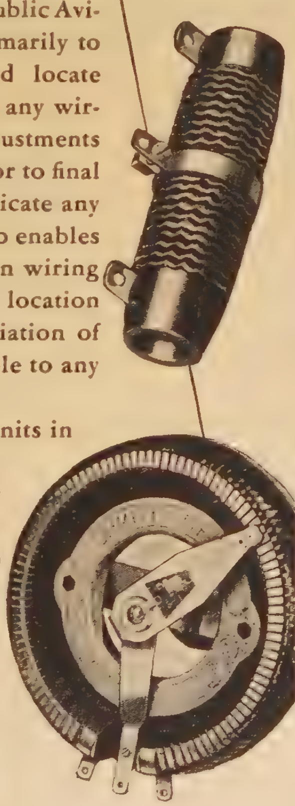
Rheostats, Resistors in Electrical Analyzer for Aircraft

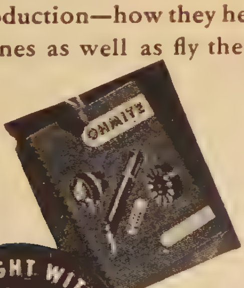


Analyzer Saves TIME for Republic Aircraft

The electrical analyzer, devised in the testing laboratories of the Republic Aviation Corp., was designed primarily to speed up aircraft testing and locate trouble without disconnecting any wiring. It also enables making adjustments on all electrical equipment prior to final assembly, for it is able to duplicate any missing circuits or loads. It also enables a direct analysis of any error in wiring assembly, indicating the exact location of circuits in error. Wide variation of power output makes it adaptable to any type of service or testing.

The use of Ohmite resistance units in this aircraft electrical analyzer is another indication of how Ohmite products help speed war production—how they help test planes as well as fly them.





BE RIGHT WITH OHMITE
Rheostats-Resistors
Tap Switches

Write on company letterhead for helpful 96-page Industrial Catalog and Engineering Manual No. 40.

OHMITE MANUFACTURING COMPANY
4803 Flournoy St., Chicago, U. S. A.

Here's More Good News About

Leadless MYCALEX THE INSULATOR

Trade Mark Reg. U. S. Pat. Off.

Insulating Material

A PRICE REDUCTION

is put into immediate effect by

MYCALEX CORPORATION OF AMERICA

Exclusive Licensee under all patents of "MYCALEX" (PARENT) Co., Ltd.

Applicable to full sheets in 7 of the 9 thicknesses

HOW MUCH? As much as 18% reduction in price of some sizes. Phone, wire or write for new price list, effective Nov. 2, 1942.

WHY? After a decade of unchanged prices, we now find that our accelerated production facilities result in lower costs. We feel that vital war materials should be made available as cheaply as possible to all users, to encourage even wider applications.

DELIVERIES? Last month we stated in various trade and technical publications that THE BOTTLE-NECK HAS BEEN BROKEN. We can supply full sheets of Leadless MYCALEX Insulating Material in large quantities, immediately. If you are using make-shift or substitute materials, change to Leadless MYCALEX Insulating Material.

SOME FACTS ABOUT MYCALEX INSULATING MATERIAL

"The most nearly perfect electrical insulator known today" is what leading engineers say about Leadless MYCALEX Insulating Material. It is a ceramic, possessing the following characteristics of mechanical strength and low-loss electrical properties, as determined by an independent laboratory:

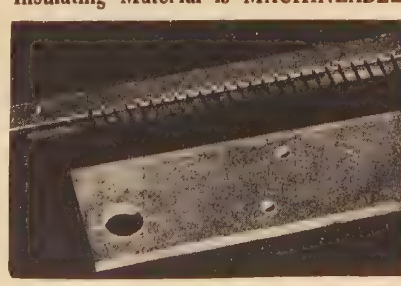
CHARACTERISTICS:

**Dielectric constant*	6.4	*Dielectric strength*	*640 volts per mil.*
**Power factor*	0.0023	*Specific gravity*	2.5
**Loss factor*	1.49	*Transverse strength*	*13,000 lbs. per sq. in.*

*Measured at 300 kilocycles after 96 hours in distilled water according to Navy Specification RE-13A-317F.

Leadless MYCALEX Insulating Material is MACHINEABLE

Your own mechanics can machine MYCALEX Insulating Material to accurate dimensions. Or, send us your specifications and let us quote on machined parts fabricated in our machine shop at our new plant in Clifton, N. J.



Write for our free illustrated booklet describing uses, machining technique, etc. Address:

MYCALEX CORPORATION OF AMERICA

60 Clifton Boulevard, Dept. 5M, Clifton, N. J.

Weston *Model 633*
A-C CLAMP AMMETER

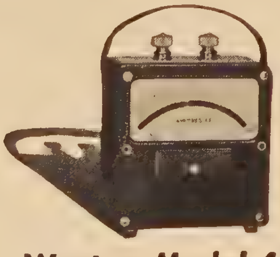
—a real *time-saver*, which tests without disturbing circuits or interrupting work. The clamping jaws are simply placed over the conductor or switch blade for current reading.

Industry's production figures bear testimony to the outstanding contribution electrical contractors and plant maintenance men are making to war-plant efficiency. Despite obstacles such as materials scarcities and limited manpower, electrical equipment—old and new—is being kept constantly fit for full scale output.

As part of the efficient maintenance combat team the WESTON test instruments illustrated—and others—are playing an important role. For in addition to their *trustworthy indications* on which maintenance has long learned to depend—they also provide features which greatly simplify preventative maintenance procedure. *In less time—at far lower cost*—industry's electrical equipment is being kept *fit* with dependable WESTONS.

Weston *Model 785*
INDUSTRIAL CIRCUIT TESTER

A highly versatile tester for trouble shooting. Provides voltage, current and resistance ranges for checking motor and control circuits, lighting circuits, sensitive relay circuits, electronic circuits, etc. (D-C sensitivity 20,000 ohms per volt.)



Weston *Model 430*
TEST INSTRUMENTS

The universal favorites for active maintenance . . . compact, rugged and with enduring precision. Large scale openings with hand calibrated mirror scales assure quick, accurate readings. Available in A-C and D-C instruments and single phase wattmeters.

Weston *Model 703*
DIRECT READING
ILLUMINATION METER

— measures all types of lighting direct, without correction factors—fluorescent, mercury vapor, incandescent, neon, daylight, etc. Made in models and ranges for shop and laboratory needs.

WESTON *Instruments*

Weston Electrical Instrument Corporation, 664 Frelinghuysen Avenue, Newark, New Jersey

More bombs for Berlin . . . more terror for Tokio

IN TOTAL WARFARE, every pound of metal we can save means just so much added destruction we can hurl at the enemy.

7000 tons of steel saved means approximately 70,000 more heavy bombs to drop on Berlin and Tokio.

2500 tons of copper saved means nearly 1,000,000 additional 3 in. A. A. cartridge cases for blasting Zeros and Messerschmitts out of the sky.

Westinghouse will save these huge amounts of steel and copper during 1942 in its Transformer Plant alone . . . because of *radical improvements in transformer materials and design* that have come out of the Westinghouse Research Laboratories and Westinghouse engineering:

An entirely new kind of transformer steel wherein the crystals line up like tenpins in a row, instead of in helter-skelter fashion as in ordinary silicon steel. This new grain-oriented steel . . . called Hipersil . . . is the result of nine years of intensive study and experiment by Westinghouse Research Engineers.

Hipersil has *one third* more flux-carrying capacity than the best grade of ordinary silicon steel. Because of its higher permeability, less Hipersil core material is needed. Transformer weight may be reduced *25 to 30 per cent.* Hipersil, alone, will save thousands of tons of critical steel and copper in the vast number of transformers Westinghouse will make during 1942.

Forced oil-cooled transformers in which the cooling medium is rapidly circulated inside the transformer shell. This means quicker heat removal. Smaller cores and coils can do a bigger job. Strategic steel and copper are saved to help win the war.

Copper temperature control. Here a liquid-cooled transformer carries a load up to the point where the copper reaches a critical temperature, beyond which the insulation may be damaged. By the use of this thermal control, higher safe loads can be carried. A transformer of a given size, therefore, does *more work* . . . another way of saving steel and copper for more shells and tanks and ships to hurl at the Axis.

Better transformers go hand in hand with these major achievements of Westinghouse "know how" in saving strategic materials for our all-out drive to victory.

Westinghouse Electric & Manufacturing Company, Pittsburgh, Pennsylvania. Plants in 25 cities. Offices everywhere.

You are cordially invited to visit the Westinghouse Electrical Exhibit at the Annual Convention of the American Association for the Advancement of Science . . . December 28-31st . . . at the Commodore Hotel, New York.

Westinghouse

. . . making Electricity work for Victory

NEW
AIEE STANDARDS

THREE REVISED STANDARDS
TWO PROPOSED STANDARDS

•

Apparatus Bushings—No. 21.—This standard which includes an appendix the "Test Code for Apparatus Bushings" approved June 24, 1942, has been available in report form since March 1941. The standard specifically covers the following types of bushings: (a) outdoor bushings for large apparatus; (b) cover type bushings for small apparatus; (c) indoor bushings for all types of apparatus except dry type instrument transformers, air-blast transformers, dry type regulators, and circuit breakers rated below 5,000 volts, and non-oiltight oil circuit breakers rated at 50,000 kva or less. Pamphlet No. 21 is sold at a cost of 40 cents per copy, less 50 per cent discount to members of AIEE on single copies.

•

Air Switches and Bus Supports—No. 22.—This revised standard, No. 22, approved June 24, 1942, supersedes the former standard of the same title. The standard covers all types of air switches and bus supports above 600 volts whether for indoor or outdoor service. It does not apply to air circuit breakers covered by standard No. 20. Pamphlet No. 22 is sold at a cost of 40 cents per copy, less 50 per cent discount to members of AIEE on single copies.

•

Switchgear Assemblies—No. 27.—This revised standard, No. 27, approved August 7, 1942, supersedes the former Standard No. 27 entitled "Switchboard and Switching Equipment for Power and Light" and its proposed revision No. 27A. These standards cover assemblies of switchgear devices such as switches, interrupting devices, control, metering, protective and regulating equipment with associated interconnections and supporting structures. These standards do not apply to industrial control equipment, communication switchboards and switching equipment, or switchboards for shipboard. Pamphlet No. 27 is sold at a cost of 30 cents per copy, less 50 per cent discount to members of AIEE on single copies.

•

Neutral Grounding Devices—No. 32.—This pamphlet is a proposed standard approved for trial use. The proposed standard applies to grounding transformers, and to resistors, reactors, capacitors, and combinations of these, when used for grounding the neutrals of alternating-current electric systems. Copies of pamphlet No. 32 can be obtained without charge.

•

Guiding Principles for the Selection of Reference Values for Electrical Standards—No. 3.—This pamphlet, a proposed standard approved for a trial period of approximately one year, is designed to serve as a guide in the preparation or revision of standards for apparatus or materials of specific types or fields of use. The values given are not mandatory, but are recommended for use in preference to other values unless there are good technical or economic reasons for using others. The values recommended have in general been selected to agree with well-established practices. Provisions are made for a great variety of conditions and for widely different apparatus and materials, some of which may be of practical importance only under unusual circumstances. Copies of pamphlet No. 3 can be obtained without charge.

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS
33 West 39th St., New York

Advanced Methods of Mathematical Analysis as Applied to Electrical Engineering

The series of five articles published in *Electrical Engineering* (February to June issues) on "Advanced Methods of Mathematical Analysis as Applied to Electrical Engineering," has been reprinted in pamphlet form.

These articles were first presented as lectures before the basic science group of the AIEE New York Section at a symposium held at Columbia University, of which Professor Paul C. Cromwell of New York University was chairman. So much interest developed that the authors were asked to make the material available to the entire Institute membership. The articles as subsequently published in *Electrical Engineering* are:

Heaviside's Direct Operational Calculus, J. B. Russell, Columbia University

Integration in the Complex Plane, K. O. Friedrichs, New York University

Laplacian Transform Analysis of Circuits With Linear Lumped Parameters, Jacob Millman, College of the City of New York

Analysis of Systems With Known Transmission-Frequency Characteristics by Fourier Integrals, W. L. Sullivan, Stevens Institute of Technology

Traveling Waves on Transmission Lines, Ernst Weber, Polytechnic Institute of Brooklyn

The purpose of the lectures, which have been published in essentially the form in which they were delivered, was to present a simplified and coordinated series on the operational and transform methods for solving differential equations encountered in the work of Electrical Engineers. For those interested in studying the basic subject matter in more rigorous and complete form, lists of references accompany each article. Copies of the 36-page, $8^1/_2$- by 11-inch pamphlet may be obtained from the AIEE order department, 33 West 39th Street, New York, N. Y., at 50 cents each; special prices will be quoted on quantity orders.

AIEE Order Department
33 West 39th Street, New York, N. Y.

Please send me:

...........Copies of "ADVANCED METHODS OF MATHEMATICAL ANALYSIS AS APPLIED TO ELECTRICAL ENGINEERING"

for which I enclose check for $..............

Name ....

Address ....

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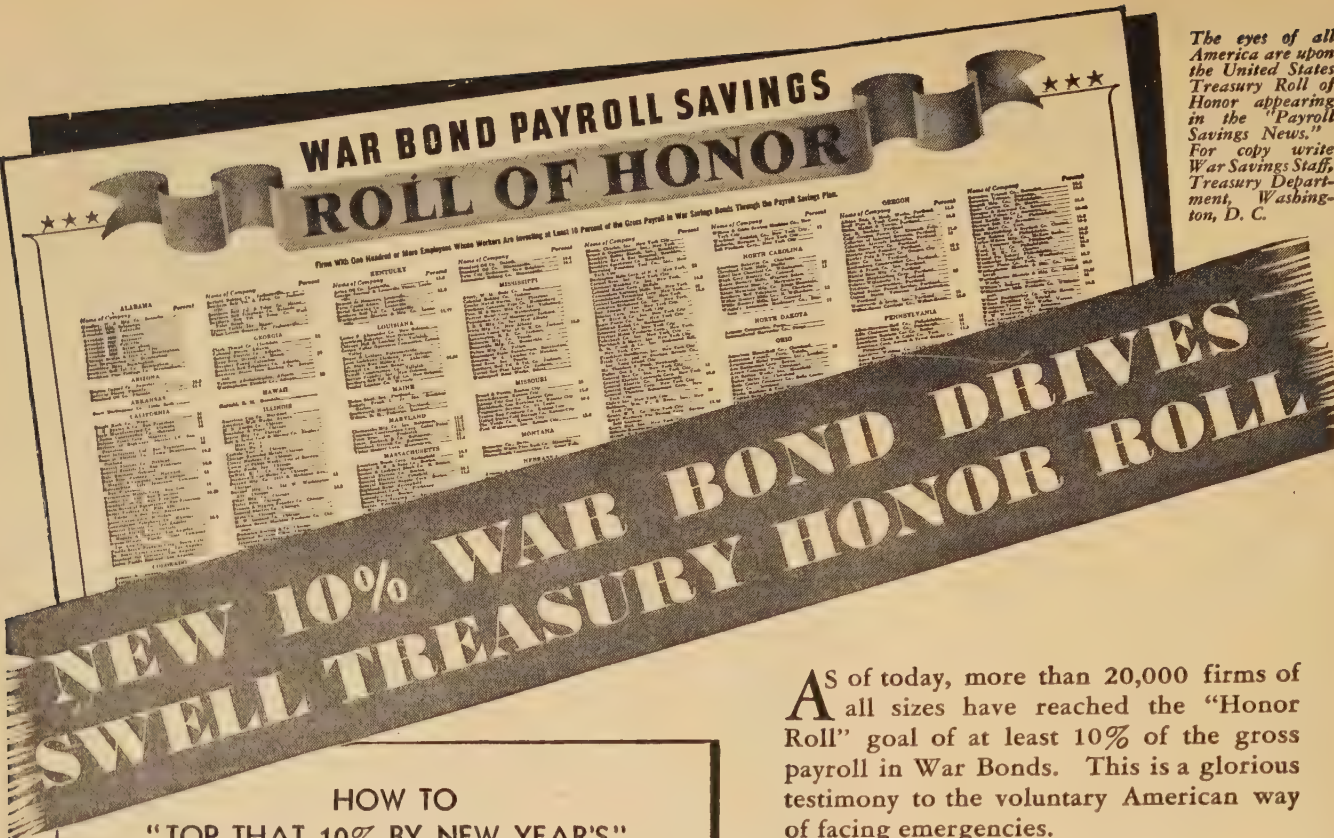
Professional Engineering Directory



The eyes of all America are upon the United States Treasury Roll of Honor appearing in the "Payroll Savings News." For copy write War Savings Staff, Treasury Department, Washington, D. C.

HOW TO "TOP THAT 10% BY NEW YEAR'S"

Out of the 13 labor-management conferences sponsored by the National Committee for Payroll Savings and conducted by the Treasury Department throughout the Nation has come this formula for reaching the 10% of gross payroll War Bond objective:

1. ***Decide to get 10%.***
   It has been the Treasury experience wherever management and labor have gotten together and decided the job could be done, the job was done.
2. ***Get a committee of labor and management to work out details for solicitation.***
   a. They, in turn, will appoint captain-leaders or chairmen who will be responsible for actual solicitation of no more than 10 workers.
   b. A card should be prepared for each and every worker with his name on it.
   c. An estimate should be made of the possible amount each worker can set aside so that an "over-all" of 10% is achieved. Some may not be able to set aside 10%, others can save more.
3. ***Set aside a date to start the drive.***
4. ***There should be little or no time between the announcement of the drive and the drive itself.***
   The drive should last not over 1 week.
5. The opening of the drive may be through a talk, a rally, or just a plain announcement in each department.
6. Schedule competition between departments; show progress charts daily.
7. Set as a goal the Treasury flag with a "T."

As of today, more than 20,000 firms of all sizes have reached the "Honor Roll" goal of at least 10% of the gross payroll in War Bonds. This is a glorious testimony to the voluntary American way of facing emergencies.

But there is still more to be done. By January 1st, 1943, the Treasury hopes to raise participation from the present total of around 20,000,000 employees investing an average of 8% of earnings to over 30,000,000 investing an average of at least 10% of earnings in War Bonds.

You are urged to set your own sights accordingly and to do all in your power to start the new year on the Roll of Honor, to give War Bonds for bonuses, and to purchase up to the limit, both personally and as a company, of Series F and G Bonds. (Remember that the new limitation of purchases of F and G Bonds in any one calendar year has been increased from $50,000 to $100,000.)

TIME IS SHORT. Our country is counting on you to—

"TOP THAT 10% BY NEW YEAR'S"

EVERYBODY EVERY PAYDAY AT LEAST 10%

Save with

War Savings Bonds

This space is a Contribution to America's All-Out War Effort by ELECTRICAL ENGINEERING

Engineering Societies **Personnel Service, Inc.**

New York 8 West 40th St. | Boston 4 Park St. | Chicago 211 West Wacker Dr. | Detroit 100 Farnsworth Ave. | San Francisco 57 Post St.

NOTICE: Due to the expansion of the various Societies in the Engineering Building, the New York Office of the Engineering Societies Personnel Service, Inc. has found it necessary to secure new quarters. These quarters are located at Eight West 40th Street and will be occupied as of December 1st, 1942. This move in no way disrupts the cooperation between the Engineering Societies and the Personnel Service.

The following items are furnished by the Engineering Societies Personnel Service, Inc., which is under the joint management of the national societies of Civil, Electrical, Mechanical, Mining and Metallurgical Engineers. This Service is available to members and is operated on a cooperative, non-profit basis.

In applying for positions advertised by the Service, the applicant agrees, if actually placed in a position through the Service as a result of these advertisements, to pay a placement fee in accordance with the rates as listed by the Service. These rates have been established in order to maintain an efficient, non-profit personnel service and are available upon request. This also applies to registrants whose notices are placed in these columns.

All replies should be addressed to the key numbers indicated and mailed to the New York Office.

A weekly bulletin of engineering positions open is available to members of the cooperating societies at a subscription of $3 per quarter or $10 per annum, payable in advance.

Positions Available

(See also page 37)

PATENT ENGINEER. Must understand electronic devices with emphasis on circuits. Salary, $6000 a year. Location, New York, N. Y. W-1311.

ENGINEERS. (a) Assistant Project Research Engineer, electrical, for development and research on radio and electrical precision instruments. Salary, $3000–$4500 a year, for a six-day week, 48-hour week. (b) Radio Designers experienced in either mechanical or electrical work. Salary, $4680 a year, six-day, 48-hour week. Location, New York Metropolitan Area. W-1329.

ENGINEERS. (a) Research and Development Engineer, either mechanical or electrical, experienced in fine precision instrument work. (b) Production Control Engineer with from three to five years' experience in precision manufacturing. (c) Tool Engineer and Designer. Salaries, $5000–$7500 a year. Location, New York, N. Y. W-1340.

ELECTRICAL ENGINEERS. (a) Electrical Foreman, supervising the work of fifteen men. Will have complete charge of maintenance and construction work in large, modern chemical plant. Salary, $3600 a year. (b) Recent Graduate Electrical Engineer with little or no experience. Will do some electrical drafting, a great deal of detail work in the office, writing requisitions for electrical materials, keeping records of electrical starters, motors and some special field work, testing and calibrating some meters, relaying, etc. Salary, $1820–$2080 a year. Location, Louisiana. W-1354.

ELECTRICAL ENGINEER for marine wiring installation. Must be qualified to direct the layout and installation aboard ship. While marine experience is desired, a capable industrial building wiring man would qualify. Permanent. Location, New York, N. Y. W-1357.

ELECTRICAL ENGINEER for testing motors and motor generating sets. Some previous experience desirable. Salary open. Location, northern New Jersey. W-1364.

RESEARCH ENGINEER for direction of an educational and development program in the acoustical and electronics field. Experience in acoustics necessary. Salary, $4500–$5000 a year. Location, New York, N. Y. W-1375.

JUNIOR ELECTRICAL ENGINEER for electronics laboratory. Should have a good mathematical background and preferably some experience in electronics. Salary, to start, $2060 a year, plus overtime. Location, New York Metropolitan Area. W-1408.

ENGINEER, 45–55, who has a good engineering background with general civil and electrical engineering experience. Should be a good administrator and executive. Salary, $5600 a year. Location, Washington, D. C. W-1410.

UTILITIES ENGINEER. Must have had a well rounded experience in the electrical utilities field. Should be capable of intelligent solution of operating, generating and distribution problems. Will deal directly with company officials and work directly under the Chief Engineer. Salary open. Location, New York, N. Y. W-1414.

GRADUATE ELECTRICAL ENGINEER to work with the engineering department of an instrument manufacturer and in cooperation with them write operating and servicing manuals. Must have had some writing experience. Permanent. Salary open. Location, New York, N. Y. W-1438.

ELECTRICAL OR MECHANICAL ENGINEER experienced in pumps, internal combustion engines, road building equipment, compressors and pneumatic tools. Salary, $4800–$6000 a year. Location, Washington, D. C. W-1513.

ELECTRICAL OR MECHANICAL GRADUATE ENGINEER, young, up to date on wage incentive work and capable of taking charge of such work within a reasonably short time. Position permanent for the right man. For 50 hours per week during war period, salary, $4000 a year; for 40 hours per week, salary, $3300 a year. Location, Minnesota. R-1162C.

Men Available

ELEC ENGR; 47, married; G E Test, Sales Engr, switchgear specialist, asst util engr. Location immaterial. Only permanent pos considered. Available reasonable notice. E-58.

ELEC ENGR available on reasonable notice. Exper includes mech and elec pwr, devpmt of inventions, technical writing and teaching, dept management and meeting public. Only permanent position considered. E-59.

ELEC ENGG GRAD, Bliss; 38, married; exper 14 yrs pub util engg, constr; 2 yrs eqpt maintenance; some inside wiring; wants permanent pos in United States or possessions. E-60.

ELEC ENGR; 34; broad maintenance and constr exper; chemical industry; exper estimating, specifications and des; present co 9 yrs; married, draft exempt; desire opportunity for wider use of ability. E-61.

E.E., 1923; married, 42; thoroughly exper engg, constr, oprn and management elec util, particularly distr, transm and hydro plants; desires responsible engg or management pos util or allied industry. E-62.

EXEC DEVPMT ENGR; 55; employed. Many patents on electro-mech apparatus and processes. Talks language of research lab and operator in metal industry. E-63.

ELEC ENGR. Long exper in pub util operation and management, devpmt of new products in the elec and non-ferrous industries; at present employed, seeks opportunity for wider use of ability. E-64.

ELEC ENGR, 20 yrs util exper, 14 yrs in Brazil with engg registration and legal residence there. Would like to make contact with American organization requiring technical representative in that country. E-65.

ADVERTISED PRODUCTS INDEX

AMMETER COMPENSATING COILS
Minerallac Electric Co., Chicago

AMMETERS, VOLTMETERS
(See INSTRUMENTS, ELECTRICAL)

ANCHORS, GUY
W. N. Matthews Corp., St. Louis, Mo.

BOXES, CABLE JUNCTION
G & W Electric Specialty Co., Chicago

BRIDGES, KELVIN-WHEATSTONE
Shallcross Mfg. Co., Collingdale, Pa.

BRUSHES, COMMUTATOR
National Carbon Co., Inc., Cleveland, O.

BUS BAR SUPPORTS
Burndy Engg. Co., Inc., New York
General Electric Co., Schenectady, N. Y.

BUS DISTRIBUTION SYSTEMS
Square D Co., Detroit, Mich.

CAPACITORS
Aerovox Corp., New Bedford, Mass.
General Electric Co., Schenectady, N. Y.
Solar Mfg. Corp., Bayonne, N. J.
Sprague Specialties Co., North Adams, Mass.
Westinghouse E. & M. Co., E. Pittsburgh

CIRCUIT BREAKERS

Air Enclosed

General Electric Co., Schenectady, N. Y.
I-T-E Circuit Breaker Co., Philadelphia
Roller-Smith Co., Bethlehem, Pa.
Square D Co., Detroit, Mich.
Westinghouse E. & M. Co., E. Pittsburgh

Oil

Allis-Chalmers Mfg. Co., Milwaukee, Wis.
General Electric Co., Schenectady, N. Y.
Roller-Smith Co., Bethlehem, Pa.
Westinghouse E. & M. Co., E. Pittsburgh

CLAMPS, CABLE SUPPORTING
Matthews Corp., W. N., St. Louis

CLAMPS, HOT LINE
Matthews Corp., W. N., St. Louis

CLIPS, WIRE
Matthews Corp., W. N., St. Louis

CONDENSERS, ELECTROSTATIC
Aerovox Corp., New Bedford, Mass.
General Electric Co., Schenectady, N. Y.
General Radio Co., Cambridge, Mass.
Solar Mfg. Corp., Bayonne, N. J.
Sprague Specialties Co., North Adams, Mass.
Westinghouse E. & M. Co., E. Pittsburgh

CONDUIT PRODUCTS
General Electric Co., Bridgeport, Conn.
National Elec. Products Corp., Pittsburgh

CONNECTORS, SOLDERLESS
Burndy Engg. Co., Inc., New York
General Electric Co., Schenectady, N. Y.
Matthews Corp., W. N., St. Louis
National Elec. Products Corp., Pittsburgh
Square D Co., Detroit, Mich.

CONTROLLERS
Allis-Chalmers Mfg. Co., Milwaukee, Wis.
General Electric Co., Schenectady, N. Y.
Rowan Controller Co., Baltimore, Md.
Square D Co., Milwaukee, Wis.
Westinghouse E. & M. Co., E. Pittsburgh

CONVERTERS, SYNCHRONOUS
Allis-Chalmers Mfg. Co., Milwaukee, Wis.
General Electric Co., Schenectady, N. Y.
Westinghouse E. & M. Co., E. Pittsburgh

CUTOUTS, PRIMARY-SEC. DISTRIB.
G & W Electric Specialty Co., Chicago
Matthews Corp., W. N., St. Louis

DRAFTING PENCILS
Dixon Crucible Co., J., Jersey City, N. J.

ELECTRONIC TUBES
General Electric Co., Schenectady, N. Y.
Westinghouse E. & M. Co., E. Pittsburgh

FURNACES, ELECTRIC
American Bridge Co., Pittsburgh
General Electric Co., Schenectady, N. Y.

FUSES, HIGH VOLTAGE
Schweitzer & Conrad, Inc., Chicago
Westinghouse E. & M. Co., E. Pittsburgh

FUSE LINKS
Matthews Corp., W. N., St. Louis

FUSE PULLERS & PLIERS
Matthews Corp., W. N., St. Louis

GASKETS
Armstrong Cork Co., Lancaster, Pa.

GENERATORS AND MOTORS
Allis-Chalmers Mfg. Co., Milwaukee, Wis.
General Electric Co., Schenectady, N. Y.
Westinghouse E. & M. Co., E. Pittsburgh

GROUND RODS
Copperweld Steel Co., Glassport, Pa.

GUARDS, GUY WIRE
Matthews Corp., W. N., St. Louis

HEATING UNITS
General Electric Co., Schenectady, N. Y.

HOUSINGS, CONTROL APPARATUS
Kirk & Blum Mfg. Co., The, Cincinnati

INSTRUMENTS, ELECTRICAL

Graphic

Ferranti Electric, Inc., New York
General Electric Co., Schenectady, N. Y.
Leeds & Northrup Co., Philadelphia
Roller-Smith Co., Bethlehem, Pa.
Westinghouse E. & M. Co., E. Pittsburgh
Weston Elec. Instrument Corp., Newark, N. J.

Indicating

Biddle Co., James G., Philadelphia
Ferranti Electric, Inc., New York
General Electric Co., Schenectady, N. Y.
Leeds & Northrup Co., Philadelphia
Roller-Smith Co., Bethlehem, Pa.
Westinghouse E. & M. Co., E. Pittsburgh
Weston Elec. Instrument Corp., Newark, N. J.

Integrating

Ferranti Electric, Inc., New York
General Electric Co., Schenectady, N. Y.
Westinghouse E. & M. Co., E. Pittsburgh
Weston Elec. Instrument Corp., Newark, N. J.

Scientific Laboratory, Testing

Acme Elec. & Mfg. Co., Cuba, N. Y.
Biddle Co., James G., Philadelphia
Du Mont Labs., Inc., Allen B., Passaic, N. J.
Ferranti Electric Inc., New York
General Electric Co., Schenectady, N. Y.
General Radio Co., Cambridge, Mass.
Leeds & Northrup Co., Philadelphia
Roller-Smith Co., Bethlehem, Pa.
Shallcross Mfg. Co., Collingdale, Pa.
Westinghouse E. & M. Co., E. Pittsburgh
Weston Elec. Instrument Corp., Newark, N. J.

INSULATING MATERIALS

Cloth

General Electric Co., Bridgeport, Conn.
Irvington Varnish & Ins. Co., Irvington, N. J.
Minerallac Electric Co., Chicago
Westinghouse E. & M. Co., E. Pittsburgh

Compounds

General Electric Co., Bridgeport, Conn.
Minerallac Electric Co., Chicago
Roebling's Sons Co., John A., Trenton, N. J.
Westinghouse E. & M. Co., E. Pittsburgh

Lava

American Lava Corp., Chattanooga, Tenn.

Moulded

Bakelite Corp., New York
General Electric Co., Bridgeport, Conn.
Square D Co., Peru, Ind.
Westinghouse E. & M. Co., E. Pittsburgh

Paper

General Electric Co., Bridgeport, Conn.
Irvington Varnish & Ins. Co., Irvington, N. J.
Westinghouse E. & M. Co., E. Pittsburgh

Steatite Ceramic Compositions

American Lava Corp., Chattanooga, Tenn.

Tape, Friction

Minerallac Electric Co., Chicago
Okonite Company, The, Passaic, N. J.
Roebling's Sons Co., John A., Trenton, N. J.

Varnishes

Dolph Company, John C., Newark, N. J.
General Electric Co., Bridgeport, Conn.
Irvington Varnish & Ins. Co., Irvington, N. J.
Minerallac Electric Co., Chicago

INSULATORS, PORCELAIN
General Electric Co., Schenectady, N. Y.
Lapp Insulator Co., LeRoy, N. Y.
National Elec. Products Corp., Pittsburgh
Ohio Brass Co., Mansfield, O.
Universal Clay Prod. Co., Sandusky, O.
Westinghouse E. & M. Co., E. Pittsburgh

INSULATOR TESTING EQUIPMENT
Ferranti Electric, Inc., New York
General Radio Co., Cambridge, Mass.
Roller-Smith Co., Bethlehem, Pa.

INSULATION TESTING EQUIPMENT
Biddle Co., James G., Philadelphia

LAMP GUARDS & CHANGERS
Matthews Corp., W. N., St. Louis

LIGHTING EQUIPMENT, OUTDOOR
General Electric Co., Schenectady, N. Y.
Westinghouse E. & M. Co., E. Pittsburgh

LIGHTNING ARRESTERS
General Electric Co., Schenectady, N. Y.
Schweitzer & Conrad, Inc., Chicago
Westinghouse E. & M. Co., E. Pittsburgh

LIGHTING GAPS
Matthews Corp., W. N., St. Louis

LOAD RECORDERS—CONTROLLERS
Leeds & Northrup Co., Philadelphia

MAGNETS, ALNICO
Arnold Engg. Co., The, Chicago

MEGOHMMETERS
Shallcross Mfg. Co., Collingdale, Pa.

METERS, ELECTRICAL
(See INSTRUMENTS, ELECTRICAL)

MOTORS
(See GENERATORS AND MOTORS)

OSCILLOGRAPHS, CATHODE RAY
Du Mont Labs., Inc., Allen B., Passaic, N. J.
General Electric Co., Schenectady, N. Y.
General Radio Co., Cambridge, Mass.
Westinghouse E. & M. Co., E. Pittsburgh

PENCILS, DRAWING
Dixon Crucible Co., J., Jersey City, N. J.

PLASTICS
Bakelite Corp., New York
General Electric Co., Schenectady, N. Y.

POLE LINE HARDWARE
Ohio Brass Co., Mansfield, O.

PORCELAIN, ELECTRIC
Square D Co., Peru, Ind.
Universal Clay Prod. Co., Sandusky, O.

POTHEADS
G & W Electric Specialty Co., Chicago

RECTIFIERS
Allis-Chalmers Mfg. Co., Milwaukee, Wis.
Federal Tel. & Radio Corp., E. Newark, N. J.
General Electric Co., Schenectady, N. Y.
Westinghouse E. & M. Co., E. Pittsburgh

REELS, PAYOUT & TAKEUP
Matthews Corp., W. N., St. Louis

REGULATORS, VOLTAGE
Allis-Chalmers Mfg. Co., Milwaukee, Wis.
General Electric Co., Schenectady, N. Y.
General Radio Co., Cambridge, Mass.
Roller-Smith Co., Bethlehem, Pa.
Sola Electric Co., Chicago, Ill.
Westinghouse E. & M. Co., E. Pittsburgh

RELAYS
Cramer Co., Inc., R. W., Centerbrook, Conn.
General Electric Co., Schenectady, N. Y.
I-T-E Circuit Breaker Co., Philadelphia
Roller-Smith Co., Bethlehem, Pa.
Westinghouse E. & M. Co., E. Pittsburgh
Weston Elec. Instrument Corp., Newark, N. J.

RELAY TEST SETS
Shallcross Mfg. Co., Collingdale, Pa.

RESISTORS
General Electric Co., Schenectady, N. Y.
General Radio Co., Cambridge, Mass.
International Resistance Co., Philadelphia
Ohmite Mfg. Co., Chicago
Shallcross Mfg. Co., Collingdale, Pa.
Sprague Specialties Co., North Adams, Mass.
Westinghouse E. & M. Co., E. Pittsburgh

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(Continued from page 40)

RHEOSTATS, LABORATORY
Biddle Co., James G., Philadelphia
General Electric Co., Schenectady, N. Y.
General Radio Co., Cambridge, Mass.
International Resistance Co., Philadelphia
Ohmite Mfg. Co., Chicago
Westinghouse E. & M. Co., E. Pittsburgh

SEALS
Armstrong Cork Co., Lancaster, Pa.

SHEET METAL PRODUCTS
Kirk & Blum Mfg. Co., The, Cincinnati

STANDARDS, RESISTANCE
Shallcross Mfg. Co., Collingdale, Pa.

STEEL, ELECTRICAL
Allegheny Ludlum Steel Corp., Pittsburgh

SWITCHBOARDS
Allis-Chalmers Mfg. Co., Milwaukee, Wis.
General Electric Co., Schenectady, N. Y.
I-T-E Circuit Breaker Co., Philadelphia
Roller-Smith Co., Bethlehem, Pa.
Square D Co., Detroit, Mich.
Westinghouse E. & M. Co., E. Pittsburgh

SWITCHES—PANELS
Square D Co., Detroit, Mich.

SWITCHES, ROTARY SELECTOR
Shallcross Mfg. Co., Collingdale, Pa.

SWITCHES, AUTOMATIC TIME
Cramer Co., Inc., R. W., Centerbrook, Conn.
General Electric Co., Schenectady, N. Y.
Minerallac Electric Co., Chicago

SWITCHES, DISCONNECT
General Electric Co., Schenectady, N. Y.
Matthews Corp., W. N., St. Louis
Roller-Smith Co., Bethlehem, Pa.
Square D Co., Detroit, Mich.
Westinghouse E. & M. Co., E. Pittsburgh

SWITCHES, GENERATOR FIELD
I-T-E Circuit Breaker Co., Philadelphia
Roller-Smith Co., Bethlehem, Pa.

SWITCHES, OIL
G & W Electric Specialty Co., Chicago

SWITCHES, SERIES LIGHTING
Matthews Corp., W. N., St. Louis

SWITCHGEAR HOUSINGS
Kirk & Blum Mfg. Co., The, Cincinnati

TIMERS, ALL TYPES
Cramer Co., Inc., R. W., Centerbrook, Conn.

TOWERS, TRANSMISSION
American Bridge Co., Pittsburgh

TRANSFORMERS
Acme Elec. & Mfg. Co., Cuba, N. Y.
Allis-Chalmers Mfg. Co., Milwaukee, Wis.
Chicago Transformer Corp., Chicago, Ill.
Ferranti Electric, Inc., New York
General Electric Co., Schenectady, N. Y.
General Radio Co., Cambridge, Mass.
Kuhlman Electric Co., Bay City, Mich.
Roller-Smith Co., Bethlehem, Pa.
Sola Electric Co., Chicago
United Transformer Co., New York
Westinghouse E. & M. Co., E. Pittsburgh

TURBINES and TURBINE GENERATORS
Allis-Chalmers Mfg. Co., Milwaukee, Wis.
General Electric Co., Schenectady, N. Y.
Westinghouse E. & M. Co., E. Pittsburgh

VARNISHES, INSULATING
Dolph Company, John C., Newark, N. J.
General Electric Co., Bridgeport, Conn.
Irvington Varnish & Ins. Co., Irvington, N. J.
Westinghouse E. & M. Co., E. Pittsburgh

WELDERS, ARC
Allis-Chalmers Mfg. Co., Milwaukee, Wis.
General Electric Co., Schenectady, N. Y.
Westinghouse E. & M. Co., E. Pittsburgh

WELDING WIRE
American Steel & Wire Co., Cleveland, O.
General Electric Co., Schenectady, N. Y.

WIRES AND CABLES
Aluminum Co. of America, Pittsburgh
American Steel & Wire Co., Cleveland, O.
Belden Mfg. Co., Chicago
Copperweld Steel Co., Glassport, Pa.
Crescent Ins. Wire & Cable Co., Trenton, N. J
General Electric Co., Schenectady, N. Y.
Kerite Ins. Wire & Cable Co., New York
National Elec. Products Corp., Pittsburgh
Okonite Company, The, Passaic, N. J.
Okonite-Callender Cable Co., Passaic, N. J.
Roebling's Sons Co., John A., Trenton, N. J.

Two Resistance Welding Aids

. . . Prepared by an AIEE Committee

Applications of resistance welding in the fabrication of industrial products have increased tremendously in the past several years, and now are contributing heavily in industry's "all out" effort to produce the sinews of war. Successful and efficient resistance-welder performance is dependent upon good electrical-machine characteristics, and an adequate power supply is one of the requirements of present-day high-speed production.

To assist power engineers, industrial plant engineers, welding machine engineers, and any others actively interested in resistance welding, in planning layouts for welder installations and in the purchase of new equipment, the subcommittee on power supply for welding operations of the AIEE committee on electric welding has prepared a three-part report entitled "Power Supply for Resistance-Welding Machines," covering:

I. **Guide to Good Electrical Performance of Resistance-Welding Machines.**
Intended to assist in the preparation of specifications for the purchase of welding machines for the best electrical design, and covers spot, projection, butt, flash, seam, and portable welders.

II. **Resistance-Welder Installation.**
Covers recommended methods of installing resistance-welding equipment, based upon a survey of a number of commercial installations throughout the United States.

III. **Factory Wiring for Resistance Welders**
Shows how properly to lay out a plant power-distribution system for serving welding machines.

The report is available in two 8½- by 11-inch printed pamphlets, Parts I and II (16 pages) in one, and Part III (12 pages) in the other. Copies of each pamphlet may be obtained at *25 cents each* from the AIEE Order Department, 33 West 39th St., New York, N. Y., payment to accompany order.

ADVERTISERS

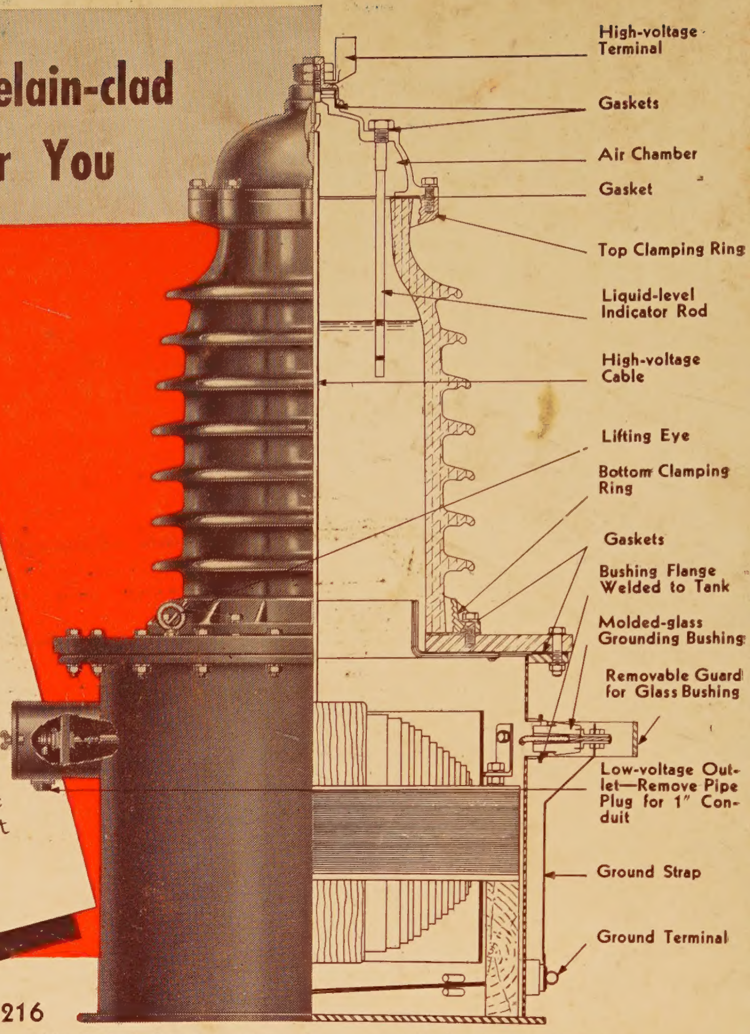
Here's What These Porcelain-clad Transformers Will Do for You

1. SAVE COPPER
Their light weight and small size (for the 69-kv unit, only 1100 lb and 4½ ft high) enable you to mount them in the steel structure and thereby reduce the length of copper runs.

2. SAVE SPACE
Mounting them in the steel structure saves valuable ground space.

3. SAVE FOUNDATION EXPENSE
Mounting in the structure also eliminates the need for constructing a separate foundation.

4. REDUCE MAINTENANCE
Hermetically sealed in, the insulating liquid need not be tested or inspected.



TYPE E-216

69-kv Single-bushing Potential Transformer (Available from 23 kv to 69 kv)

and you get these excellent operating characteristics!

ACCURACY—Well within the highest accuracy classification—¼W, ¼X, ¼Y, ¼Z—these transformers are suitable for both metering and relaying—line to neutral, on grounded and ungrounded circuits.

INSULATION—Impulse levels meet the test requirements established by ASA Standards.

IMPULSE FLASHOVER—The porcelain housing acts as a high-voltage bushing and has the same 60-cycle and impulse-flashover characteristics as the conventional-type bushing. For example, the following data apply to the 69-kv size:

60-cycle, dry, 1-minute-withstand test—175 kv
60-cycle, wet, 10-second-withstand test—145 kv
Full-wave-impulse-withstand test—350 kv

You can adopt G-E instrument transformers as standard throughout your system, for General Electric makes a complete line—indoor and outdoor, current and potential, and portable types.

For additional information about the E-216 or other types, address the nearest G-E representative. *General Electric, Schenectady, N. Y.*

For the "Last Ounce" of Protection SPECIFY PYRANOL

Because these transformers are such a vital link in the protection of your system, you'll want to insist on Pyranol—the G-E insulating and cooling liquid which is chemically stable and will not sludge. Pyranol also means greater safety, for it is noninflammable and adds little to the price of the transformer.

The Navy "E", for Excellence, has been awarded to 92,780 General Electric employees in five plants manufacturing naval equipment

GENERAL ELECTRIC

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